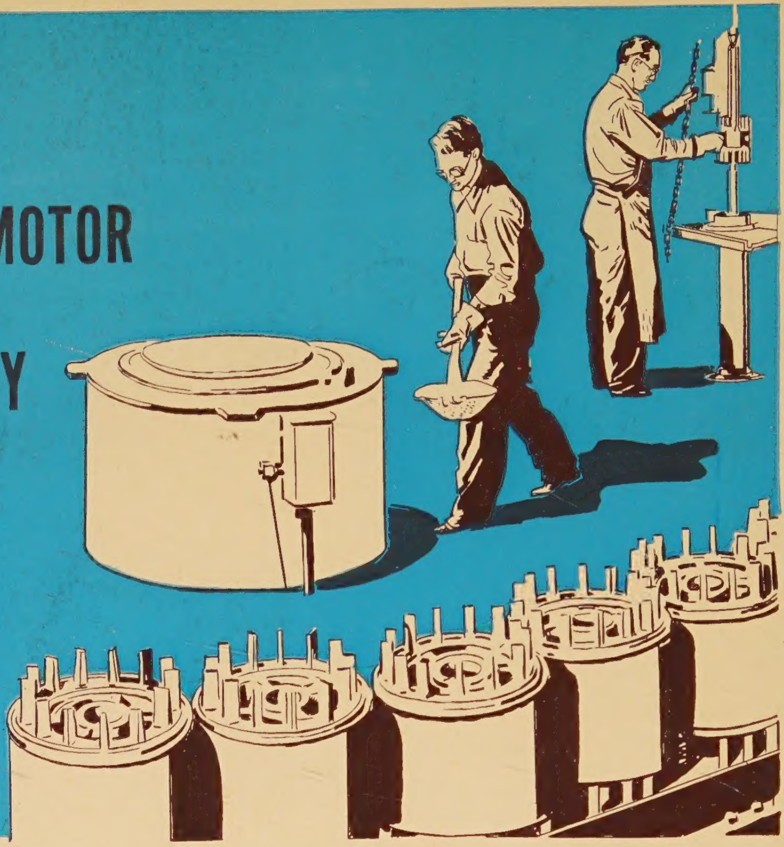
ELECTRICAL ENGINEERING

JUNE

1943

AIEE NATIONAL TECHNICAL MEETING, CLEVELAND, OHIO, JUNE 21-25, 1943

WHY BURDEN A MOTOR WITH UNNECESSARY DEADWEIGHT?



Weight-saving wasn't the aim of the Imagineer who first used cast aluminum rotors in his electric motors. He chose aluminum alloys of different electrical properties to vary the operating characteristics of his squirrel-cage motors. Today, this is standard practice with a number of manufacturers. It simplifies and speeds assembly and it cuts costs.

Now, isn't it time to be doing some Imagineering that has weight-saving as its purpose? Lighter motors may give you an inside track on many a postwar market. No need to take time off from vital war production; include it in your eighth-day thinking.

Aluminum rotors are just a start in the right direction. Weight-saving can undoubtedly be accomplished by using aluminum alloys for other motor parts. Steel inserts can be cast integrally with the aluminum, providing necessary electrical and physical properties and, at the same time, cutting machining costs. The superior heat conductivity of aluminum may even make higher horsepower ratings possible.

The performance of aluminum in power conductors, for years past—over a million miles—long ago proved its dependability. Forward-looking electrical designers, who are visualizing aluminum in new fields, have this evidence to support their Imagineering. Aluminum Company of America, 2149 Gulf Building, Pittsburgh, Pennsylvania.

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1943

The Cover: Wartime guides for the application and operation of these and other types of electrical apparatus will be a major topic at the AIEE national technical meeting in Cleveland, Ohio, June 21–25. Headquarters will be at the Hotel Cleveland (*pages 255–8*).

Photos courtesy Westinghouse, General Electric, and Ohio Brass companies, and Lieutenant John C. Beckett, USNR

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SALUTE TO THE WOMEN OF TOMORROW!

Science will eliminate more and more household drudgery

RESEARCH AND INVENTION have been speeded up by the war — the tragedies of today will, in the not-too-far-distant future, be transformed into the blessings of peace.

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The Engineer and the War

H. S. OSBORNE
PRESIDENT AIEE

The engineer's relationship to the war effort and his responsibilities in that effort both as a citizen and as a professional man are subjects of interest to all engineers at the present time.

THE subject of the engineer and the war is an intimate one and associates what are perhaps the two things we are most interested in at the present time; ourselves, the engineers, and the great struggle which is now in full blast to determine the future destinies of the world.

The subject is also a generous one. The highly mechanized character of the war and the tremendous importance in the war effort of production and of engineering and scientific development are such that there can be no question as to the essential relation of the engineer and the war. There is then a wide range of subject matter from which to choose in our discussion of the topic.

One possible approach to the subject of the engineer and the war is to discuss the accomplishments of the engineer in contributing to the war effort. These accomplishments have been very great. It is, of course, necessary that they should be great. Starting practically from nothing, 49,000 military planes were produced in 1942. In 1937 there were 10 shipyards with 46 ways, capable of making ships 400 feet long or over. Now, there are 60 yards with 300 ways, with a total capacity of 20,000,000 dead-weight tons of shipping annually. In the major classes of military needs the production in 1942 runs from $3^1/_2$ to more than 6 times the 1941 production. The production of 1943 will again be a large step up from that of 1942.

One of the most striking figures is the horsepower of airplane engines required for the planes produced in the United States. Up to the end of 1943, considering only the number of engines necessary to equip the planes, and without allowance for a large percentage of spare engines, this will total 350,000,000 horsepower. This is probably from $1^1/_2$ to 2 times the total installed central-station power capacity of the world.

These figures speak of accomplishments in which the work of the engineer has been a large and essential part. I speak of them briefly, however, because accomplishments are things done and hence belong to the past. This is a time when I believe we wish to think more about the present and the future.

To some, the consideration of our subject in relation to the present may raise the question of the status of the engineer in the war effort. Is the engineer accorded adequate recognition for his contribution? Does he occupy a position of leadership in the war effort which the importance of engineering in the war would justify? A recent letter received from an engineering group illustrates this point of view. They request that the Institute join them in urging that engineers be delegated to sit at the peace negotiations.

Throughout my professional life I have heard from time to time discussions of the status of the engineer. It seemed to me that this whole question was put in proper perspective by a discussion more than 25 years ago by George F. Swain, in a joint meeting of engineering societies met to consider this topic. Professor Swain said, in effect, that if we believe the engineer has not received proper recognition, let us look to the engineer and his training for the causes for that lack of recognition and the remedies. If the engineer does not lead, said Professor Swain, it must be for the reason that he lacks the personal qualities of leadership, and, if he lacks these qualities, he ought not to lead. The question then comes back to ourselves, to study in what ways, individually and as a group, the engineer's capacity of taking a broader part in the affairs of the world can be increased. If this point of view is accepted, it disposes adequately of this phase of the subject.

If we turn now to the consideration of our subject in relation to the future, I believe it will suggest, at once, to many the responsibilities of the engineer in the war effort.

The war brings increased responsibility to all, as citizens and as workers. This is an inevitable result of times which are crucial in the highest degree—times in which there are many more things to be done and more important things to be done than in any ordinary time. I do not propose to develop this thought further, but mention it, because, while we discuss here particularly our responsibilities as engineers, we must not forget our more general responsibilities as citizens.

When we say that the engineer has a particularly close relation to the war because of the nature of the war effort, we must at the same time recognize that this means also particularly great responsibilities. What are some of these responsibilities?

Obviously, one responsibility of the engineer is to direct his specialized knowledge of engineering to the war effort to maximum advantage. He should be doing

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Essential substance of an address presented at the opening session of the AIEE South West District technical meeting, Kansas City, Mo., April 28, 1943.

the kind of work to which he can contribute his utmost. To some this may mean changing from what they are doing to some other work. To most it means continuing on one's present job, but doing it better than he has ever done it before. All this seems to be very elementary in principle, though very complex in individual application.

Many engineers have a responsibility in this matter, not only to themselves as individuals, but to the engineering profession as a whole—a responsibility of helping to insure that engineers be correctly assigned in war work or in the armed forces, and that adequate engineering training be given to young men to insure a continued supply of necessary engineering talent to the armed forces and to industry. I believe that, with the recent developments in Army and Navy training courses, the preferred status of the engineer in the draft, and other recent developments, progress is being made in these matters.

A responsibility of the engineer, closely related to the one we have just discussed, is to keep his mind open and his imagination active to the impact of changing conditions. We all develop, in our work individually and in our organizations, engineering habits. They are great conservators of time and effort and are as necessary to efficient work as the ordinary habits of the individual are to living. We are now, however, in a period of changed and rapidly changing conditions. The engineering habits of yesterday, at least many of them, do not meet the conditions of today. We need to restore in our own minds and in our organizations that habit of questioning every practice, however well-established, which is often the characteristic of a bright young man right out of college. I hope that we will question with more judgment than some of us did when we had just received the coveted diploma. The impact of the scarcity of materials on engineering practices, which is the subject of so much discussion in the Institute meetings this year, is a single illustration of this necessity of questioning and challenging our engineering practices.

In view of the close relation of the engineer to the war effort, he has, I believe, a special responsibility for civilian morale. The engineer is a man "in the know." He may be considered by the nonengineering citizen to be even more "in the know" than he really is. At any rate, his engineering knowledge tends to make him more influential than he may realize in determining the attitude of others toward the uncertainties, the inconveniences, and difficulties, and toward the optimisms and pessimisms which follow each other in rapid succession as the war flows on around us.

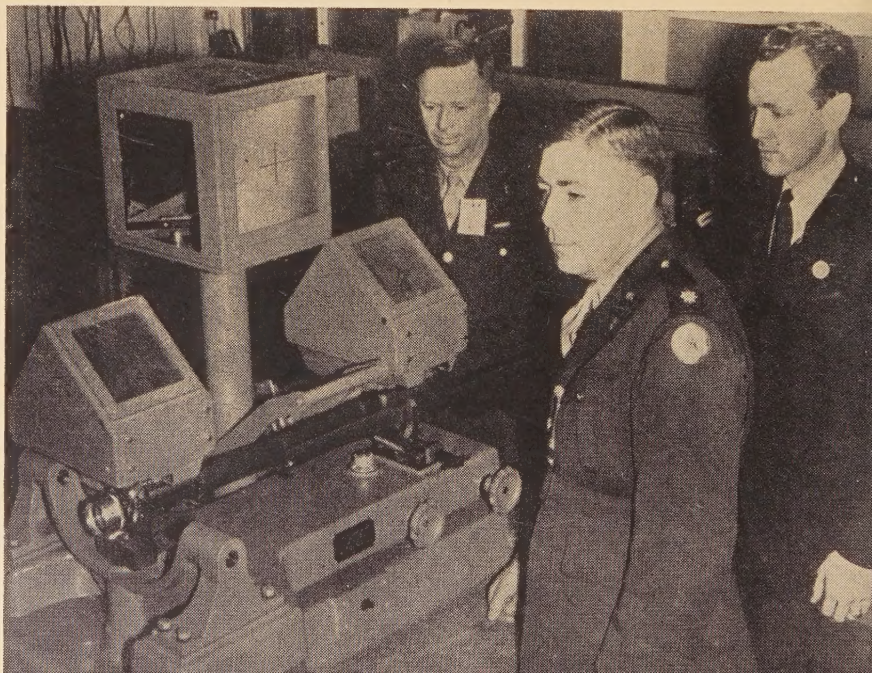
Finally, I believe it is the responsibility of the engineer even now to plan ahead. I say even now, because naturally our first responsibility is to give everything we have to advancing the war effort. But we want the war effort and the peace effort which will follow it not only to be effective now, but to continue to be effective throughout the years which will follow. And one essence of engineering is that best results can be obtained only by planning ahead.

Perhaps in speaking of planning ahead, I should first say that we should plan the planning. There is now a tremendous lot of talk going on and a good deal of effort being expended, which relates to the planning of industry for the post war period. The first step is to determine what planning should be done now and what planning should be postponed until a later period.

I am of the opinion that there are no general truths as to when we should start with postwar planning and how fast we should go. I believe, however, that each of us, whether connected with manufacturing, with utilities, with education, or with other aspects of engineering, should seriously consider the subject from the standpoint of his own engineering activities, and determine what are the things which should come first and when those should be done. The postwar world will be full of problems for all of us and it is certainly a necessary part of sound engineering to make forward-looking plans early enough so that they will be ready when needed.

Of course, I have not touched upon all of the major responsibilities of the engineer in these war times. I hope that these illustrations, however, are sufficient to focus in the minds of all of us the point of view that the engineer does have a special relationship to the war effort and, accordingly, has a special responsibility. If we keep this point of view before us as we think of the engineer and the war effort, we cannot go wrong.

Precision-sighting of Garand semiautomatic rifles in quantity production for the United States armed forces can now be done with mirrors and without firing a shot through the use of this optical rifle-sighting gage developed by General Electric Company. The equipment, which was conceived by N. F. Barnes and K. R. Geiser (A '38) of the general engineering laboratory, saves up to 13 rounds of ammunition formerly used in sighting each gun; permits the job to be done in less than two minutes per rifle by one worker, half the time it formerly required two; and takes less room than the average kitchen instead of the 100-yard rifle range needed by the former method



Ultrashort Electromagnetic Waves

IV—Guided Propagation

S. A. SCHELKUNOFF
MEMBER AIEE

When Doctor S. A. Schelkunoff presented the material contained in this article as a lecture before the basic science group of the New York Section, he remarked that it was "nonmathematical." Certainly the electrical engineer will welcome any concept which allows an easier approach to the solution of certain problems involved in wave guides than the more complete equivalent field-theory method. In this fourth article in a series of six on ultrashort electromagnetic waves, Doctor Schelkunoff combines transmission-line theory with optical analogy and derives useful relations for both wave guides and cavity resonators. The three preceding articles appeared in the March, April, and May issues of "Electrical Engineering."

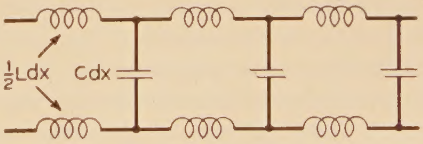
PAUL C. CROMWELL, Chairman, Symposium Committee

(College of Engineering, New York University, New York, N. Y.)

WHATEVER difference there is between the theory and practice of microwaves on the one hand and that of long waves on the other is due to the size of the transmission structures relative to the wave length. Anyone familiar with communication engineering will find the fundamentals of microwave transmission easy to understand, because no new ideas are required; but he should become used to keeping the order of magnitudes in mind and be prepared to find that some conventional types of structures are no longer practicable because of the smallness of their size. The communication engineer's fundamental ideas do not have to be revised, because they are based on the laws of electromagnetic induction which govern the behavior of all electromagnetic waves at high and low frequencies alike. For a review of these laws and their application to transmission lines, the reader is referred to the first two papers in this series.[1,2]

There are two methods of approach to the problem of wave propagation, either along ordinary two-conductor transmission lines or along more general "wave guides" which include single-conductor lines. Both are based on the same fundamental laws of electromagnetic induction and are in a certain sense complementary, each possessing its advantages and disadvantages. One of these methods may be called the "engineering method," because in the past it has been particularly favored by engineers. The other is the "field-theory" method. But these names should not be taken seriously, and we certainly do not wish to imply that the engineers have ignored field considerations. The engineering method is based on the integral equations of induction, although the engineer hides the fact by preferring to express these equations in a verbal form; the second method is based on the differential equations which readily follow from the integral equations. Both methods of solving problems can be exact; but it so happens that it is easier to solve the differential equations in those few instances in which they can be solved exactly, and it is far easier to obtain good approximate solutions of the integral equations when solving the differential equations appears to be a thankless task. The engineer, who is concerned with complicated situations, has picked the best available mathematical method. Consider, for example, a telephone transmission line. For a pair of infinite parallel perfectly conducting straight wires, we can find a simple exact solution of the differential equations. But the actual telephone line is not infinite, is not straight, and is not perfectly conducting; in fact, it is not even a pair of parallel wires but a twisted pair or a transposed pair. Under the circumstances, the problem of solving Maxwell's differential equations is hopeless; but the integral equations, particularly when supplemented by a few measurements, lead at once to a highly satisfactory approximate solution.

Figure 1. A conventional diagram representing a two-conductor transmission line



I am emphasizing the true nature of the engineering method, because at times it has been said quite unjustifiably that this method is essentially approximate and somehow inferior to the field-theory method.

On the other hand, when the geometry of the transmission system is simple, it is easy to solve the differential equations; the scope of application of these solutions can then be augmented by using them in conjunction with the integral equations.

S. A. Schelkunoff is consultant in electromagnetic theory and member of the technical staff of Bell Telephone Laboratories, Inc., New York, N. Y.

The concepts of resistance, conductance, inductance, and capacitance are very fundamental in that they are based on energy considerations and are quite independent of the frequency. The resistance and conductance are merely the coefficients in the expressions for the electric power transformed into heat; the inductance and capacitance are the coefficients in the expressions for the magnetic and electric energies. When these energies are concentrated in small regions, the precise distribution is unimportant, and integrated values may be used; but generally they are distributed. Hence we have the conceptions of *distributed* resistance, conductance, inductance, and capacitance in "long circuits" or "transmission lines." It is no coincidence that the dimensions of the conductivity, the permeability, and the dielectric constant of a substance are those of conductance per-unit length, inductance per-unit length, and capacitance per-unit length. Through these conceptions of the so-called "circuit theory," electromagnetic oscillations and waves may be treated as a special case of general Lagrangian dynamics. The ideas of resistance, inductance, and capacitance, simple as they are, belong to advanced dynamics.

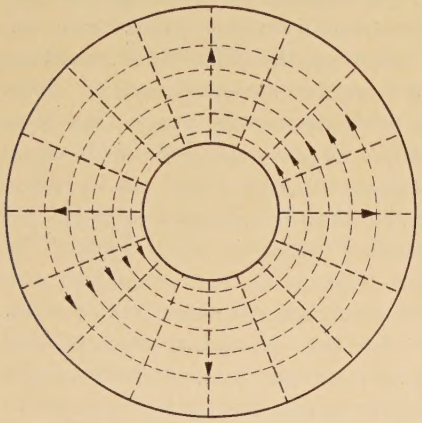


Figure 2. Lines of force in a coaxial transmission line

TRANSVERSE ELECTROMAGNETIC WAVES

In the case of transmission lines comprised of two independent conductors, it is possible for the nearby sections of these conductors to be oppositely charged and for electric lines to go from one conductor to the other. Magnetic lines surround either one or the other conductor, and we have a wave which is described as a transverse electromagnetic wave, for the reason that the electric and magnetic intensities are both perpendicular to the wires and hence to the direction of wave propagation. The transverse voltage between the wires produces a displacement or capacitance current "in shunt" with the line, and this makes the longitudinal conduction current in the wires vary from point to point. The changing (with time) transverse magnetic field makes the transverse voltage vary from point to point along the line. Thus the voltage and current are propagated along the line, and the nature of this propagation is represented conventionally by the diagram in Figure 1, subject to the interpretation that the series inductance and shunt capacitance are continuously distributed.

The series impedance and shunt admittance per-unit length are respectively

$$\mathcal{Z} = R + j\omega L, \qquad Y = R + j\omega C \tag{1}$$

where $\omega = 2\pi f$ is the frequency in radians per second.

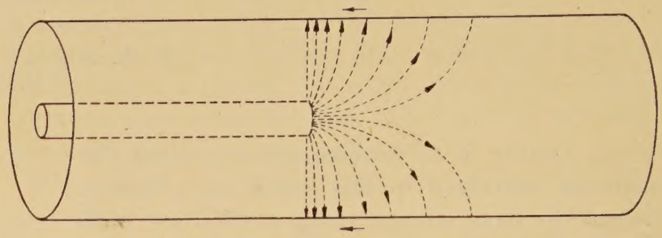


Figure 3. Electric lines of force at low frequencies near the end of the inner conductor of a coaxial pair

For the time being we shall ignore the series resistance due to the conductors and the shunt conductance due to imperfect dielectric between them. In practice these quantities deliberately are kept so small that it is possible to deal with the problem in two steps, that is, to neglect the effect of energy dissipation to begin with, and then to reintroduce it as the first-order correction. The propagation constant is then

$$\Gamma = \sqrt{\mathcal{Z}Y} = j\omega\sqrt{LC} \tag{2}$$

In a traveling wave, the voltage, current, and field are proportional to $e^{-\Gamma x}$ where x is the distance along the line. Since Γ is imaginary, the absolute value of this exponential factor is unity at all frequencies, and only the phase changes along the line. Of course, energy losses in the resistance will cause a slow attenuation of the amplitude; but this will be dealt with later.

If the dielectric between the conductors is homogeneous, then $LC = \mu\epsilon = 1/v^2$ where v is the velocity characteristic of the medium. In vacuum $v \simeq 3 \times 10^8$ meters per second. Thus we also write

$$\Gamma = \frac{j\omega}{v} = j\frac{2\pi}{\lambda} \tag{3}$$

where λ is the wave length.

The actual values of L and C depend on the field configuration, and those for parallel pairs, for example, are different from those for coaxial pairs. There may also be different *transmission modes* corresponding to different field configurations and hence to different line constants (L, C) consistent with the same physical structure. If a pair of wires is surrounded by a cylindrical shield, symmetric about the pair, there are two obvious transmission modes. In one mode the currents in the wires are equal and similarly directed, and the return current is in the shield; in the other mode the currents in the wires are equal but oppositely directed.

The wave velocity depends only on the product LC; the ratio L/C becomes important when we come to consider the power carried by the wave.

TRANSVERSE MAGNETIC WAVES

We are all familiar with that mode of propagation in a coaxial pair in which electric lines are radial, and magnetic lines are circles (Figure 2). Let us see what happens if we terminate the inner conductor (Figure 3).

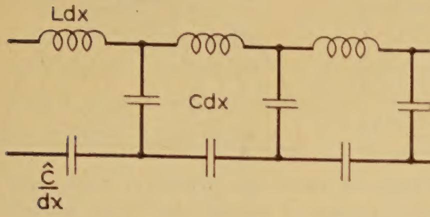


Figure 4. A diagram representing the transmission properties of transverse magnetic waves in wave guides

The electric lines may be expected to bulge out as some charge will be induced on the outer cylinder beyond the end of the inner cylinder. The electric field beyond the terminated end will possess a certain amount of energy and may be represented by a small capacitance C_t between the end of the inner conductor and the outer conductor. At low frequencies the impedance $1/j\omega C_t$ is exceedingly large, and the coaxial pair is practically open in the electrical sense, as well as in the mechanical; but as the frequency increases, this impedance decreases, and the displacement current in the hollow cylinder increases. Now the displacement current, just like the conduction current, is surrounded by magnetic lines; thus with increasing frequency the magnetic field in the hollow cylinder increases, and we are no longer justified in ignoring it. Stronger charging currents will flow in the cylinder, and we begin to realize that the hollow tube is also a kind of transmission line in which the conduction current in the tube returns as the displacement current. The transmission diagram is therefore that shown in Figure 4 in which the series capacitance is included. Those familiar with filters will recognize the structure as a high-pass filter.* At low frequencies the capacitive series impedance dominates the inductive, and the whole structure is capacitive; at high frequencies the inductive reactance dominates, and the structure becomes like the one shown in Figure 1. The effective inductance is diminished by the capacitance, and the wave velocity is increased. The two conditions occur respectively for frequencies less than and greater than the frequency at which the series capacitance resonates the series inductance; at this critical frequency, the series impedance and the propagation constant are reduced to zero.

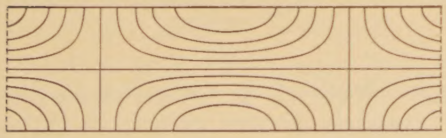


Figure 5. Electric lines of force for the $TM_{0,1}$ wave

The series impedance and shunt admittance are

$$Z = j\omega L + \frac{1}{j\omega\hat{C}}, \qquad Y = j\omega C$$

The propagation constant is then

$$\Gamma = \sqrt{\left(j\omega L + \frac{1}{j\omega\hat{C}}\right) j\omega C} = \sqrt{-\omega^2 LC\left(1 - \frac{1}{\omega^2 L\hat{C}}\right)} \qquad (5)$$

When ω is small, the quantity under the square root sign is positive, and Γ is real; the field is attenuated at the rate Γ nepers per meter. But when ω is large enough, Γ becomes imaginary, and the field is no longer attenuated. The critical or *cutoff frequency* is given by

$$\Gamma = 0, \quad \omega_c = \frac{1}{\sqrt{L\hat{C}}} \qquad (6)$$

Below this frequency the wave is attenuated, even though there is no loss of energy; this means that the energy merely fluctuates between the source (the coaxial pair in Figure 3) and the tube. Above the cutoff some energy passes into the tube and travels along it.

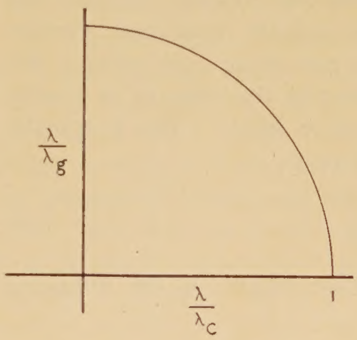


Figure 6. The relationship between the free-space wave length λ, the wave length in the guide λ_g, and the cutoff wave length λ_c

An idea of the order of magnitude of ω_c and the corresponding wave length λ_c may be obtained from the following simple consideration. The series inductance L, representing flux linkages with the longitudinal displacement current, is independent of the area S of the cross section. The series capacitance $\hat{C}$, on the other hand, is proportional to S. Hence ω_c is inversely proportional to $\sqrt{S}$, and λ_c is directly proportional to $\sqrt{S}$ or to the radius of the tube. The exact value of $\hat{C}$ depends on the exact distribution of the longitudinal displacement current. For a rough approximation, assume that the distribution is uniform; then $\hat{C} = \epsilon S$, and† $L = \mu/8\pi$. Substituting in equation 6, we have $\omega_c = 2\sqrt{2}\, v/a$, and $2\pi a = 2\sqrt{2}\,\lambda_c = 2.82\lambda_c$, where a is the radius of the cylinder. A much better approximation may be obtained by assuming a distribution which conforms to the known fact that the longitudinal electric intensity, and therefore the displacement density, must vanish at the conducting boundary. However, for circular tubes the exact value is available, namely $2\pi a = 2.40\lambda_c$.

Introducing ω_c in equation 5 we have

$$\Gamma = j\omega\sqrt{LC}\sqrt{1 - \frac{\omega_c^2}{\omega^2}} \qquad (7)$$

---

* It is a band-pass filter when L, $\hat{C}$, C are lumped and a high-pass filter when they are continuously distributed as in the present case.

† The inductance is then the same as for a wire carrying uniform current.

Assuming that $\omega > \omega_c$ we write

$$\Gamma = j\frac{2\pi}{\lambda_g} \tag{8}$$

where λ_g is the wave length in the guide. This length is the span within which the phase takes on all values between 0 and 360 degrees. The wave length λ_g is infinite at the cutoff and then steadily decreases with increasing ω. Every half wave length, the field is reversed and electric lines cannot go on extending as shown in Figure 3; they must assume the form shown in Figure 5 where the distance between successive clusters of lines is $\lambda_g/2$. Magnetic lines are circles linked with electric lines.

The wave velocity in the guide is

$$v_g = f\lambda_g \tag{9}$$

it is infinite at the cutoff and is seen to approach a constant value $1/\sqrt{LC}$ as the frequency increases. But then λ_g becomes small, the electric loops contract, and the field becomes substantially transverse electromagnetic over the greater part of the cross section. We expect the velocity to become the same as in coaxial pairs, that is, the "velocity of light." This is actually the case and we may now write

$$\Gamma = \frac{j\omega}{v}\sqrt{1-\frac{\omega_c^2}{\omega^2}} = j\frac{2\pi}{\lambda}\sqrt{1-\frac{\lambda^2}{\lambda_c^2}} \tag{10}$$

where $\lambda = v/f$ is the wave length in the unlimited medium. The ratio

$$\frac{\lambda}{\lambda_g} = \sqrt{1-\frac{\lambda^2}{\lambda_c^2}} \tag{11}$$

if plotted against λ/λ_c, is represented by an arc of a circle of unit radius (Figure 6).

Below the cutoff Γ is real and

$$\Gamma\lambda_c = 2\pi\sqrt{1-\frac{\lambda_c^2}{\lambda^2}} = 2\pi\sqrt{1-\frac{f^2}{f_c^2}} \tag{12}$$

The plot of $\Gamma\lambda_c/2\pi$ versus λ_c/λ or f/f_c is also a quadrant of a unit circle. Since λ_c is fixed by the dimensions of the tube, the plot represents the variation in Γ.

This wave is no longer completely transverse; only the magnetic intensity is transverse, and for this reason the wave is called a *transverse magnetic* wave.

Suppose now we begin deforming the cross section of the tube. Magnetic lines, unable to penetrate the conductor, will adjust their shape to the new boundary. In tubes of rectangular and semicircular cross section the lines assume the form shown in Figure 7. The field tends to avoid corners; hence we may expect that the effective area of the cross section is smaller than the actual area and that the cutoff wave length is shorter than for a circular tube of the same cross-sectional area.

Imagine a circular tube divided into two halves by a conducting partition along a diameter (Figure 8), and let waves of equal intensity be excited in each half in such a way that magnetic lines are oppositely directed. The conduction currents in the two sides of the partition are also equal and oppositely directed; hence, their effect on the field is canceled, and the partition can be removed without disturbing the field. Thus we obtain another mode of transmission in the circular tube. The cutoff wave length is proportional to the square root of one half of the cross-sectional area and is smaller therefore than for the previous transmission mode. It becomes evident that there will be an infinite number of possible transmission modes. It is particularly easy to enumerate them in tubes of rectangular cross section. Divide the tube into *mn* cells as shown in Figure 9. In each cell establish a wave of the type shown in Figure 7, taking care that the polarities in adjacent cells are opposite. If the intensities of these waves are equal, the partitions may be removed without disturbing the field. This wave is called a $\text{TM}_{m,n}$† wave and it may be launched by taking *mn* parallel wires which carry oppositely directed currents as shown diagrammatically in Figure 9, and which are abruptly terminated at some cross section of the tube. The cutoff frequencies evidently increase as *m* and *n* increase. The values of *L*, $\hat{C}$, *C* depend on the shape and size of the cross section of the tube and on the particular mode.

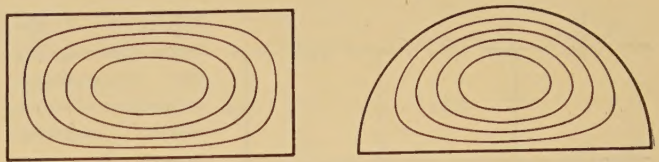


Figure 7. Magnetic lines of force for principal transverse magnetic waves in wave guides of noncircular cross section

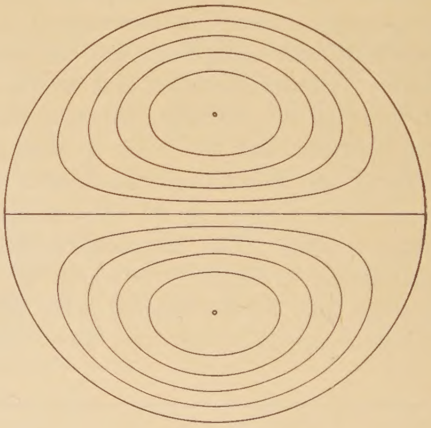


Figure 8. Magnetic lines of force for a $\text{TM}_{1,1}$ wave in a circular guide

TRANSVERSE ELECTRIC WAVES

Consider now a transmission line comprised of two parallel wires. Suppose that we insert a conducting loop *ABC* in shunt with the wires as shown in Figure 10. At low frequencies this loop is a practical short circuit, of course; but, as the frequency increases, the impedance of the loop increases, and we have merely a shunt inductance added to the existing shunt capacitance. By placing a succession of such loops along the line, we change the structure from that of Figure 1 to that of Figure 11. A continuous succession of loops on either side of the parallel pair transforms it into a hollow tube (Figures 12 and 13).

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† TM—Transverse magnetic.

At low frequencies the shunt inductive impedance is low, the capacitors are bypassed, and the whole structure is an inductance; but at high frequencies the inductive impedance is high, and the structure becomes similar to that in Figure 1, with the effective capacitance diminished by the parallel inductance. The cutoff frequency which separates these two frequency ranges is that at which the shunt admittance vanishes.

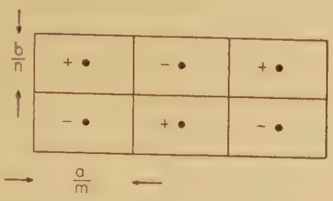

Figure 9. A diagram illustrating the generation of a $TM_{m,n}$ wave in a rectangular guide

The series impedance and shunt admittance are

$$\mathcal{Z}=i\omega L, \qquad \mathcal{Y}=i\omega C+\frac{1}{i\omega\hat{L}} \tag{13}$$

The final form for the propagation constant is the same as for transverse magnetic waves, as given by equation 10. Above the cutoff the wave length in the guide is longer than the free-space wave length to which it is related by equation 11; below the cutoff Γ is real and is given by equation 12.

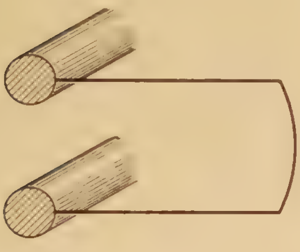

Figure 10. A pair of wires with a conducting loop in shunt with them

Whereas in the case of transverse magnetic waves the conduction current in the tube is longitudinal, in the present case it has also a transverse component. The transverse conduction and displacement currents are naturally 180 degrees out of phase.

The electric intensity is transverse as shown in Figures 14 and 15, but the magnetic intensity has a longitudinal component associated with the transverse current. Magnetic lines form loops (Figure 16) surrounding electric lines. Such waves are called *transverse electric* waves.

In a rectangular tube these waves may be produced by a small doublet antenna placed parallel to the short side. By placing two such antennas energized 180 degrees out of phase we may set up a wave with electric lines as shown in Figure 17. Evidently the cutoff frequency for this wave will be double that of the wave in Figure 14. Similarly we may produce *m* field reversals. Still another mode of transmission may be generated with four doublets as shown in Figure 18; the arrows on the doublet show the relation between the currents in them at any given instant. If the doublets are not balanced, we may have waves of the preceding types superimposed on that of Figure 18. Out of this field pattern all higher mode patterns are obtained by imagining the guide divided into cells as in Figure 9.

Having enlarged our picture to show the great variety of transmission modes possible in a hollow tube, we must now realize that such modes are possible also in coaxial pairs. These modes have remained unnoticed in conventional practice, because their cutoff frequencies are high, and in low frequency practice they manifested themselves only as "end effects."

FIELD DISTRIBUTION

In order to couple a two-conductor transmission line to a generator, we merely connect their respective terminals. Consequently, we are not particularly interested in the field intensities *E* and *H* and are concerned chiefly with their integrated values, the transverse voltage between the wires, and the longitudinal current in them. But there are many ways in which a generator may be coupled to a hollow tube; for example a small antenna or a small loop might be used and might be inserted at different points of the tube. For this reason we become interested in the details of field distribution. Following the line of thought taken in the preceding sections, we can obtain a satisfactory qualitative idea of field distribution, and this method is the only one available when the cross section is not one of the few simple shapes amenable to exact calculation.

It is beyond the scope of this paper to go into details of the general case. For a $TE_{1,0}$ wave[†] in a rectangular guide the exact field may be calculated as follows: When the magnetic intensity *H* is eliminated from Maxwell's equations, it is found that each Cartesian component of the electric intensity *E* must satisfy the wave equation

$$\frac{\partial^2 E}{\partial x^2}+\frac{\partial^2 E}{\partial y^2}+\frac{\partial^2 E}{\partial z^2}=-\frac{4\pi^2}{\lambda^2}E \tag{14}$$

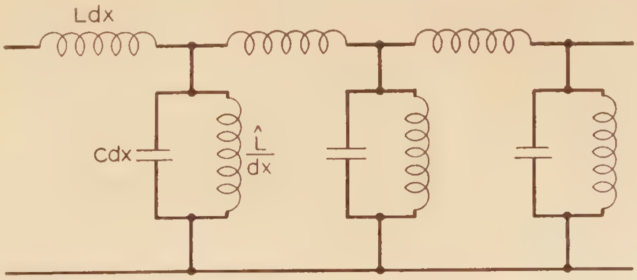

Figure 11. A diagram representing the transmission properties of transverse electric waves in wave guides

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[†] TE—Transverse electric.

In the wave under consideration we have only one component of E. Assuming that the x axis is along the longer side of the rectangle (Figure 14) and the y axis along the shorter, we observe that E_y is independent of the y co-ordinate. Since E_y is also proportional to $e^{-\Gamma z}$, so that its second derivative with respect to z is $\Gamma^2 E_y$, we have from equation 14

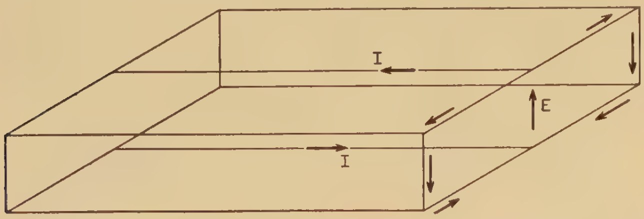

Figure 12. Relative directions of the longitudinal and transverse conduction and displacement currents in a rectangular wave guide for the TE$_{1,0}$ wave

$$\frac{\partial^2 E_y}{\partial x^2}+\Gamma^2 E_y=-\frac{4\pi^2}{\lambda^2}E_y$$

or

$$\frac{\partial^2 E_y}{\partial x^2}=-\left(\Gamma^2+\frac{4\pi^2}{\lambda^2}\right)E_y \tag{15}$$

This equation indicates that E_y is a sinusoidal function of x. Since E_y must vanish on the boundaries, $x=0$, and $x=a$, it must be proportional to sin $(m\pi x/a)$ where m is an integer; thus

$$E_y=E_0 \sin\frac{m\pi x}{a}e^{-\Gamma z} \tag{16}$$

For the TE$_{1,0}$ wave $m=1$.

Substituting from equation 16 in equation 15, we have

$$\frac{m^2\pi^2}{a^2}=-\Gamma^2-\frac{4\pi^2}{\lambda^2}, \qquad \Gamma=\sqrt{\frac{m^2\pi^2}{a^2}-\frac{4\pi^2}{\lambda^2}} \tag{17}$$

The cutoff wave length of the TE$_{m,0}$ mode is then

$$\lambda_{m,0}=\frac{2a}{m}, \qquad \lambda_{1,0}=2a \tag{18}$$

Knowing E_y, we can obtain H_x and H_z from Maxwell's equations; thus

$$H_x=-K_z E_y, \qquad H_z=-\frac{m\pi E_0}{i\omega\mu a}\cos\frac{m\pi x}{a}e^{-\Gamma z} \tag{19}$$

where

$$K_z=\frac{i\omega\mu}{\Gamma} \tag{20}$$

When $m=1$, the maximum E is in the central plane as indicated by the density of the lines in Figure 14. The transverse H, being proportional to E, is similarly distributed; hence it is maximum in the central plane and vanishes at the walls parallel to E. At the surface of each wall this H component determines the longitudinal conduction current; the latter is confined therefore to the walls perpendicular to E and is sinusoidally distributed. The longitudinal H is also sinusoidally distributed except that it vanishes in the central plane; the distribution of the transverse conduction current conforms to this H component. In the walls parallel to E the current is exclusively transverse.

IMPEDANCE

In ordinary communication engineering, the line equations may be derived first in terms of distributed constants L and C; but soon these constants recede into the background, and the attention is centered on the secondary or derived constants, the propagation constant Γ and the characteristic impedance K. The latter may be defined with reference to a progressive wave along the line as the voltage/current ratio (V/I); it may also be defined on the power-current basis or on the power-voltage basis as in the following equations

$$K_{V,I}=\frac{V}{I}, \qquad W=\frac{1}{2}K_{W,I}II^*, \qquad W=\frac{VV^*}{2K^*_{W,V}} \tag{21}$$

In these equations V is the transverse voltage, I is the total longitudinal current, W is the complex flow of power, and the asterisk indicates conjugate complex numbers so that II^*, for example, is the square of the current amplitude. For transverse electromagnetic waves

$$K_{V,I}=K_{W,I}=K_{W,V}=K \tag{22}$$

but otherwise these three impedances are different.

Above the cutoff W is real, and therefore the K's are real; then we may write the above definitions simply as

$$K_{V,I}=\frac{V}{I}, \qquad W=\frac{1}{2}K_{W,I}I^2, \qquad W=\frac{V^2}{2K_{W,V}} \tag{23}$$

Evidently we have

$$K_{V,I}=\sqrt{K_{W,I}K_{W,V}} \tag{24}$$

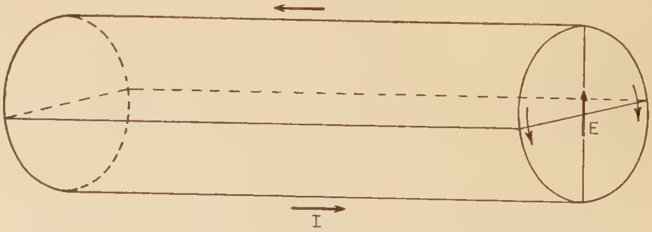

Figure 13. Relative directions of the longitudinal and transverse conduction and displacement currents for the TE$_{1,1}$ wave in a circular wave guide

In wave guides we are also concerned with field intensities, and the most basic of all impedances is defined as the ratio of the transverse field components

$$K_z=\frac{E_t}{H_t} \tag{25}$$

In computing K_z the positive directions of E_t and H_t are so chosen that the real part of K_z is never negative.

The average power carried by the guided wave is obtained by integrating $1/2(E_t \times H_t^*) = 1/2(K_z H_t H_t^*)$ over the cross section of the guide. Thus all integrated impedances are expressible in terms of the wave impedance K_z. Above the cutoff for all transverse magnetic waves we have

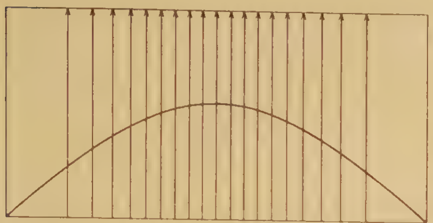

Figure 14. Distribution of transverse electric lines for $TE_{1,0}$ waves

$$K_z = \sqrt{\frac{\mu}{\epsilon}\left(1-\frac{f_c^2}{f^2}\right)} = 377\sqrt{1-\frac{f_c^2}{f^2}} \tag{26}$$

and for all transverse electric waves

$$K_z = \sqrt{\frac{\mu}{\epsilon}}\left(1-\frac{f_c^2}{f^2}\right)^{-1/2} = 377\left(1-\frac{f_c^2}{f^2}\right)^{-1/2} \tag{27}$$

The numerical coefficients are for wave guides free from dielectric matter.

For the $TE_{1,0}$ wave in a rectangular guide we find

$$\left.\begin{aligned} K_{V,I} &= \frac{\pi b}{2a} K_z = 592\frac{b}{a}\left(1-\frac{\lambda^2}{4a^2}\right)^{-1/2} \\ K_{W,I} &= \frac{\pi^2 b}{8a} K_z = 465\frac{b}{a}\left(1-\frac{\lambda^2}{4a^2}\right)^{-1/2} \\ K_{W,V} &= \frac{2b}{a} K_z = 754\frac{b}{a}\left(1-\frac{\lambda^2}{4a^2}\right)^{-1/2} \end{aligned}\right\} \tag{28}$$

where a and b are the lengths of the sides respectively perpendicular and parallel to E. The voltage V is the maximum voltage.

For the $TE_{1,1}$ wave in a circular guide we have

$$\left.\begin{aligned} K_{V,I} &= 520\left(1-\frac{\lambda^2}{\lambda_c^2}\right)^{-1/2} \\ K_{W,I} &= 354\left(1-\frac{\lambda^2}{\lambda_c^2}\right)^{-1/2} \\ K_{W,V} &= 764\left(1-\frac{\lambda^2}{\lambda_c^2}\right)^{-1/2} \end{aligned}\right\} \tag{29}$$

For the $TM_{0,1}$ wave[†] in a circular guide

$$\left.\begin{aligned} K_{V,I} &= 48\sqrt{1-\frac{f_c^2}{f^2}} \\ K_{W,I} &= 30\sqrt{1-\frac{f_c^2}{f^2}} \\ K_{W,V} &= 77\sqrt{1-\frac{f_c^2}{f^2}} \end{aligned}\right\} \tag{30}$$

The voltage V is the transverse voltage from the axis to the boundary.

For the $TM_{1,1}$ wave[‡] in a rectangular guide

$$\left.\begin{aligned} K_{V,I} &= \frac{30\pi ab}{a^2+b^2}\sqrt{1-\frac{f_c^2}{f^2}} \\ K_{W,I} &= \frac{15\pi^3 ab}{8(a^2+b^2)}\sqrt{1-\frac{f_c^2}{f^2}} \\ K_{W,V} &= \frac{480ab}{(a^2+b^2)\pi}\sqrt{1-\frac{f_c^2}{f^2}} \end{aligned}\right\} \tag{31}$$

The voltage V is the transverse voltage from the axis of the guide to the boundary; in all the above cases I is the total longitudinal current.

All these impedances are of interest in problems involving gradual changes in the size of the cross section; in order to minimize reflections the characteristic impedance should be kept constant. If the shape of the cross section remains the same, it does not matter which of the integrated impedances is used. When the shape changes, the picture becomes more complicated. Generally speaking, the integrated impedances based on the power flow are more useful than the voltage/current ratio.

Finally, we are particularly interested in the impedance seen by the coupling device, since this impedance governs the transition of power from the device into the wave guide. Such impedances have to be calculated (or measured) for each type of coupling.

ATTENUATION

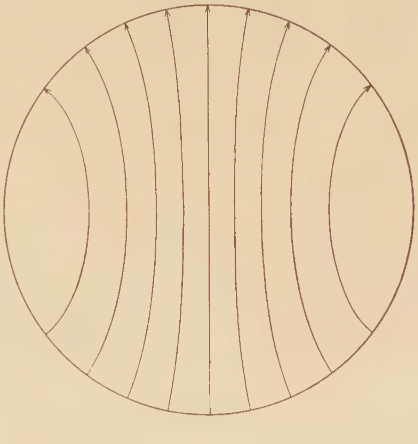

Figure 15. Distribution of electric lines for $TE_{1,1}$ waves

When an electric current flows through an imperfect conductor, a heating effect takes place, and the energy transformed into heat is abstracted from the energy traveling along the line. In the case of a $TM_{0,1}$ wave in a circular guide, for instance, the resistance R per-unit length of the outer conductor is the same as in a coaxial pair, since the current distribution is the same. The power lost in a length dz of the tube is then $1/2(RI^2)dz$; and if W is the power carried along the guide, then

$$dW = -\frac{1}{2}RI^2 dz \tag{32}$$

[†] The wave with circular magnetic lines.

[‡] Corresponds to the $TM_{0,1}$ wave in a circular guide.

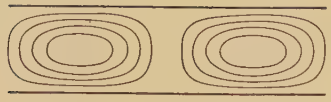

Figure 16. Magnetic lines of force for $TE_{1,0}$ waves in a rectangular wave guide and for $TE_{1,1}$ waves in a circular wave guide

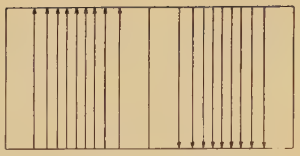

Figure 17. Distribution of electric lines of force for $TE_{2,0}$ waves

Dividing this by W as given in terms of I by equation 23, we have

$$\frac{dW}{W}=-\frac{R}{K_{W,I}}dz \qquad (33)$$

where the ratio $R/K_{W,I}$ is independent of z. Hence W is an exponential function of z

$$W=W_0\exp\left(-\frac{R}{K_{W,I}}z\right) \qquad (34)$$

The power is proportional to the square of the current, and therefore

$$I=I_0e^{-\alpha z}, \qquad \alpha=\frac{R}{2K_{W,I}} \qquad (35)$$

The exponent α is called the attenuation constant.

In this derivation, we have assumed that the power absorbed by the conductor is a small fraction of that carried by the wave, and that consequently the field distribution and $K_{W,I}$ are not significantly affected. In the immediate vicinity of the cutoff where $K_{W,I}$ is nearly zero, the preceding formula is not good. In this region a formula for α may be obtained by noting that dissipation effects are due to resistances in series with the inductances (in Figures 1, 4, 11) when power is dissipated in conductors, and to conductances in parallel with the capacitances when the loss of power takes place in the dielectric. For the $TM_{0,1}$ wave, the propagation constant at the cutoff is then

$$\Gamma=\sqrt{j\omega RC_{W,I}}=\alpha(1+j), \qquad \alpha=\sqrt{\frac{1}{2}\omega RC_{W,I}} \qquad (36)$$

In the case of transverse electric waves, the attenuation constant has two terms, one representing the power loss due to the longitudinal current, and the other the power loss due to the transverse component. Sufficiently above the cutoff the first term is proportional to $\sqrt{f}$ and the second to $f^{-1/2}$ as may be seen from equation 13.

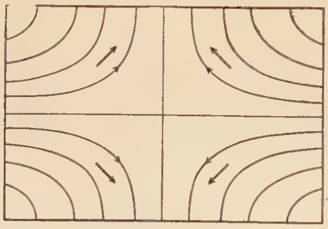

Figure 18. Distribution of electric lines of force for $TE_{1,1}$ waves

The attenuation is high close to the cutoff, because very little power is carried by the wave; as the frequency increases, the attenuation first decreases because of the increased power carried by the wave; then the latter reaches a constant value, and the attenuation becomes proportional to the surface resistance of the conductor and therefore increases as $\sqrt{f}$.

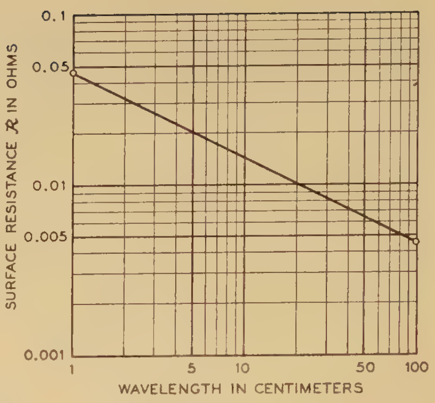

Figure 19. Surface resistance of copper conductors as a function of the wave length

Generally $\mathcal{R}=\sqrt{\pi\mu f/g}$ *ohms where f is the frequency, μ the permeability in henrys per meter, and g the conductivity in mhos per meter*

The attenuation may be expressed conveniently in terms of the resistance $\mathcal{R}$ of a unit area of the conductor (Figure 19). Thus for transverse magnetic waves in a circular tube we have

$$\alpha=\frac{\mathcal{R}}{a\sqrt{\frac{\mu}{\epsilon}}}\left(1-\frac{f_c^2}{f^2}\right)^{-1/2}$$

$$=\frac{\mathcal{R}}{377a}\left(1-\frac{f_c^2}{f^2}\right)^{-1/2} \text{ nepers per-unit length} \qquad (37)$$

For a $TE_{1,1}$ wave in a circular tube we have

$$\alpha=\frac{\mathcal{R}}{a}\left[3.76\left(1-\frac{f_c^2}{f^2}\right)^{-1/2}-2.65\left(1-\frac{f_c^2}{f^2}\right)^{1/2}\right]10^{-3} \qquad (38)$$

and for a $TE_{1,0}$ wave in a rectangular tube we have

$$\alpha=\frac{\mathcal{R}}{377}\left(\frac{1}{b}+\frac{2f_c^2}{af^2}\right)\left(1-\frac{f_c^2}{f^2}\right)^{-1/2} \qquad (39)$$

For a more complete list of attenuation formulas, see reference 4.

The attenuation constant does not actually become either infinite or even large at f_c. Thus for the $TM_{0,1}$ wave in an empty circular tube at that frequency for which the series reactance per-unit length vanishes, $\alpha=\sqrt{\mathcal{R}/60a\lambda}$. This frequency is $f_1=f_c(1-\mathcal{R}a/346\lambda_c)$, where f_c is the cutoff frequency when $\mathcal{R}=0$. For frequencies lower than f_1, the attenuation is practically independent of $\mathcal{R}$ and is given by equation 12. There is only a very narrow frequency range in the vicinity of f_1 and f_c where neither equation 12 nor equation 37 is applicable; the point corresponding to f_1 may thus be used for joining the two attenuation curves.

REACTORS IN WAVE GUIDES

When a capacitor or an inductor is placed in shunt with an ordinary transmission line, it causes a local storage of energy. Part of the energy traveling along the line is withdrawn from it during one half of the cycle and returned during the other. In effect we have a virtual generator† which sends waves in both direc-

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† A generator which on the average contributes no power.

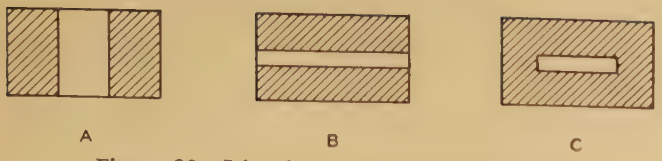

Figure 20. Irises in rectangular wave guides

tions; the wave traveling back to the actual generator is called the reflected wave. More generally a reflection takes place at any impedance discontinuity.

Similar conditions exist at impedance discontinuities in hollow tubes. Consider, for example, a rectangular guide, and introduce a metal partition to block partially the passage of the wave. If the frequency is between the lowest cutoff frequency and the next higher, the energy is transmitted over large distances in only one mode ($TE_{1,0}$). The current in the partition will produce other modes as well, but these are attenuated with the distance from the partition and thus will represent a local storage of energy. When the edges of the partition are parallel to the electric vector, as in the arrangement shown in Figure 20A, the current may flow freely from top to bottom, the local field is largely magnetic, and the partition behaves as an inductance. In the arrangement shown in Figure 20B, charges are accumulated on the upper and lower parts of the partition, the local field is largely electric, and we have a capacitor. In the arrangement shown in Figure 20C the reactance may be either inductive or capacitive, depending on the relative dimensions of the slit. The shunt inductance may actually resonate the shunt capacitance, thus making the shunt impedance infinite at some particular frequency; then the partition will not be seen by the wave which will pass through the slit without reflection.

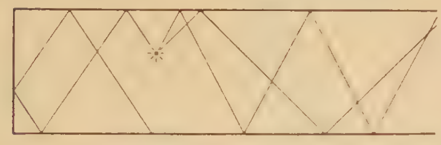

Figure 21. A source of light inside a metal tube

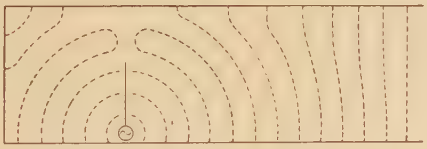

Figure 22. An antenna inside a metal tube

The conditions become different when the frequency exceeds the second cutoff frequency. Whatever fraction of the energy passing the partition happens to be taken from the first mode and transformed into the second is lost to the first mode. The partition will then act not only as a reactive discontinuity, but also as a positive resistance to the original incident wave and a negative resistance to the second mode. A similar situation exists if an antenna is inserted in shunt with a parallel-wire transmission line; the radiated energy is lost as far as the plane wave guided by the wires is concerned, and at the same time we have a source of the energy carried by the spherical wave emitted from the antenna.

OPTICAL ANALOGIES

In the foregoing discussion of wave guides the transmission-line aspect has been emphasized for a perfectly obvious reason: the wave guides are of practical interest, because they are transmission systems. In the initial stages, however, when we are trying to become familiar with an unfamiliar phenomenon, any connection with something we think we know well is very helpful. Even the most elementary books in physics discuss light waves and uniform plane waves, and most everyone knows that antennas radiate energy which may be picked up somewhere else. We shall now look at wave guides from a point of view which is particularly congenial to the physicist and acceptable to the radio engineer, but alien to the wire-communication engineer, and at best is quite limited when one wants to make a full use of wave guides as transmission systems.

Imagine a source of light inside a metal tube. Waves are emitted in all directions and are reflected in some such fashion as that indicated in Figure 21. The emitted energy will be directed† along the tube, although the walls of the tube will absorb some of it. Thus some electromagnetic waves can be transmitted through a hollow tube. But is the transmission possible at "engineering frequencies" which are so much lower than light frequencies? Let us think then of an antenna radiating between the surface of the earth and the Kennelly-Heaviside layer. There we think of sky waves bouncing back and forth between the layer and the earth in a way similar to that in Figure 21. We now take the ground, improve it by making it of copper, and wrap it around the antenna to form a tube, Figure 22. We know that if the antenna is short compared with the wave length, very little energy is radiated for a given current in the antenna. The antenna must approach a quarter wave length before a substantial amount of energy is radiated, and we conclude that the diameter of the tube should not be much less than $\lambda/4$. This picture, however, does not give us a precise idea with regard to the critical frequency; in fact, it gives us no indication that, if the diameter is smaller than 0.6λ, *the antenna will not radiate.*

There are other differences between light waves and guided waves. In the case of the former, we think of the tube merely as a boundary keeping light from escaping the tube; our attention is concentrated on the medium inside the tube. If there is a narrow slit in the tube, some light escapes; but we feel that the orientation of the slit is not important. On the other hand in the wave guide (Figure 12), the orientation of the slit is very important. A longitudinal slit in the middle of the broad top face will interfere with the flow of current but slightly, and its effect on the wave will be small; but a longitudinal slit in either of the side faces will interrupt the transverse current and produce a considerable effect. As the frequency increases, this effect becomes smaller, because the transverse current becomes smaller. Transverse slits will act in just the opposite way: the effect is small for slits in side faces and large for slits in the other two faces. A light wave would always be reflected from a transverse partition (Figure 20), but we saw that a $TE_{1,0}$ wave could pass through a narrow window. Light energy seems to be radiated equally well whether the tube is twice as large or twice as small—impedance considerations are nowhere in sight; but these considerations are of paramount importance in the construction of high-quality transmission systems.

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† The word "directed" is now perhaps a better description of what happens than the term "guided."

The optical theory breaks down for this reason: Light waves are of such high frequency that they are above the cutoff for a very large number of transmission modes, and hence the energy is propagated in all these modes; but wave guides are operated at frequencies at which only one mode or at most a few modes partake in transmission of energy.

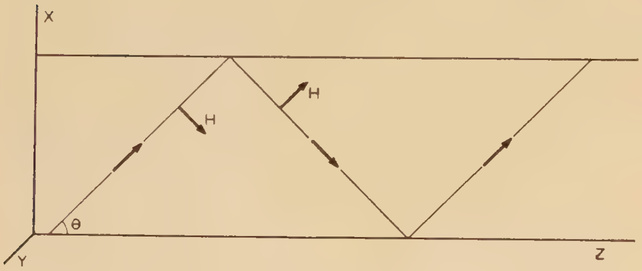


Figure 23. Illustrating a uniform plane wave reflected back and forth between two perfectly conducting planes

We shall not consider the relationship between guided waves and the uniform plane waves on which the wave theory of light is based. Imagine two parallel metal planes whose cross section is shown in Figure 23 and an ordinary plane wave, with E parallel to the metal boundaries. At each boundary this wave is reflected in such a way that the total E vanishes. This means that the electric intensities of the incident and reflected waves are equal and opposite. Thus at the origin the electric intensity will be of the following form

$$E_y = E_0 \exp\left[-\frac{2\pi j}{\lambda}(x\sin\theta + z\cos\theta)\right] -$$

$$E_0 \exp\left[-\frac{2\pi j}{\lambda}(-x\sin\theta + z\cos\theta)\right] \qquad (40)$$

$$= -2jE_0 \sin\left(\frac{2\pi}{\lambda}x\sin\theta\right)\exp\left(-j\frac{2\pi}{\lambda}z\cos\theta\right)$$

When $x=a$, E_y must also vanish; therefore

$$\frac{2\pi a}{\lambda}\sin\theta = m\pi,\quad m=1, 2, 3, \ldots \qquad (41)$$

$$\sin\theta = \frac{m\lambda}{2a}$$

If this value of $\sin\theta$ is inserted in equation 40, we obtain equation 16.

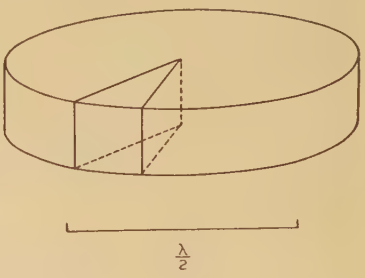


Figure 24 (right). A cylindrical cavity resonator

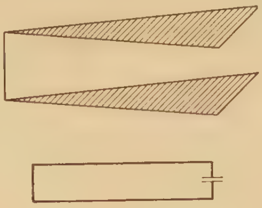


Figure 25 (below)

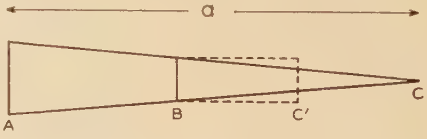
A $TE_{m,0}$ wave may be regarded therefore as a zigzagging uniform plane wave. We have connected a less familiar type of wave with a more familiar one, and we are apt to feel that we now understand the former better, that is, if we do not press the matter too far. Equation 41, for instance, says that a plane wave is "permitted" to zigzag only at specified angles, depending on the wave length; that if $\lambda=1.5a$, there is only one permitted angle; and that if $\lambda=3a$, so that $\sin\theta>1$, there are no permitted waves at all. Of course, if we ignore the physical significance of equation 41 and eliminate $\sin\theta$ from equation 40, things are set right; then, however, we lose the individual plane waves with which we started.

Such embarrassing questions do not arise in the case of light waves whose wave lengths are of the order of 2.27×10^{-5} inches so that if $a=1$ foot, let us say, there would be about 1,000,000 permitted angles. Small random variations in a will make all angles permitted.

CAVITY RESONATORS

Cavity resonators are sections of wave guides,[†] closed at both ends to form metal boxes in order to prevent energy escaping to free space. In this sense they behave as sections of transmission lines, short-circuited at both ends. Like the latter, cavity resonators possess an infinite number of resonant frequencies. To each transmission mode there will correspond a set of oscillation modes, corresponding to an integral number of half wave lengths (the guide wave lengths) along the guide; but some of these oscillation modes will be duplicated.

Figure 26



A pillbox (Figure 24) is a simple cavity resonator. It may be regarded either as a section of a cylindrical guide or as a section of a "disk transmission line" formed by two parallel planes. In the latter case we may start with a source on the axis and think of a cylindrical wave spreading outward. Consider a particular wave in which the E lines run from one plane to the other. This wave will be reflected back toward the axis and interfere with the original wave to form a standing wave. Our first idea may be that at resonance the radius will be equal to $\lambda/4$; but then we shall recall that this result is obtained for transmission lines with uniformly distributed capacitance and inductance, whereas for the cylindrical wave under consideration the capacitance per-unit radius increases with the distance from the axis. This is particularly evident if we cut a thin "pie" out of the "cake," Figure 24. We now have a pair of wires open at one end and short-circuited at the other. In considering waves in this pair of wires we should think of electric lines going straight between the triangular conductors, since this is what happens in the box on account of the proximity of the adjacent wedge-shaped sections. The nonuniformity of the capacitance distribution should alter the effective length of the transmission line. If we have an excess of capacitance near the open end (Figure 25), the section of the line is made effectively longer, and the resonant wave length will be greater than four times the actual length of the section. The capacitance deficiency near the short-circuited end does not have much effect, because the voltage is small there. Returning to our section of the pillbox, we find the capacitance deficiency at the open end which makes the line effectively shorter. The resonant wave length will then be less than four times the actual length of the section. Pursuing the matter further, we should say that (Figure 26) when $BC=a/\sqrt{2}=0.707a$ and $AB=0.293a$, the capacitance is evenly divided. Of course, in estimating the effect of uneven capacitance distribution on the resonant wave length, we should give due weight to the voltage distribution. Since, however, the voltage varies slowly near the open end, we shall ignore this variation. Thus we shall replace the triangular piece of length $BC=0.707a$ by a rectangular piece of the same area and hence of length $BC'=0.354a$. The effective length of our nonuniform line becomes then $AC'=0.646a$, and the resonant wave length $\lambda=4AC'=2.59a$. The exact value to two decimal figures is $\lambda=2.62a$. Our result is almost as good as we can get from the formula

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[†] Of either uniform or nonuniform cross section.

$$\lambda=4a\sqrt{\frac{1+\xi}{1-\xi}}, \qquad \xi=\frac{\int_0^a C\cos\frac{\pi x}{a}\,dx}{\int_0^a C\,dx} \tag{42}$$

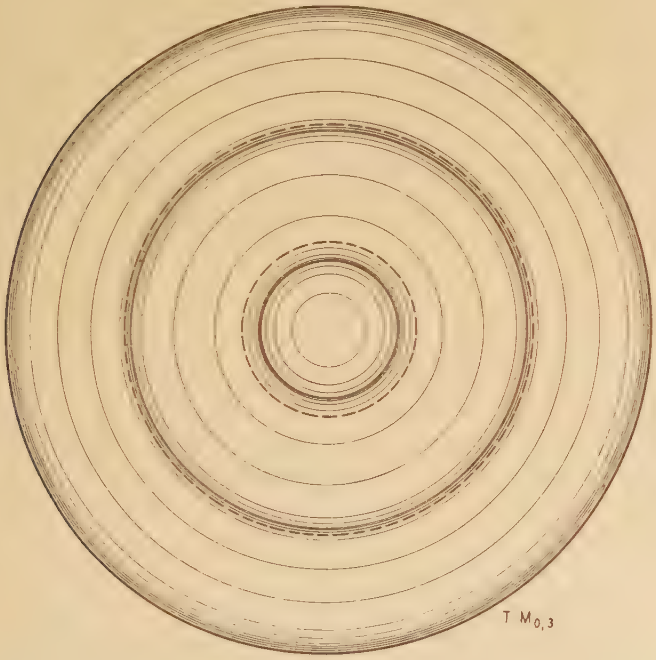
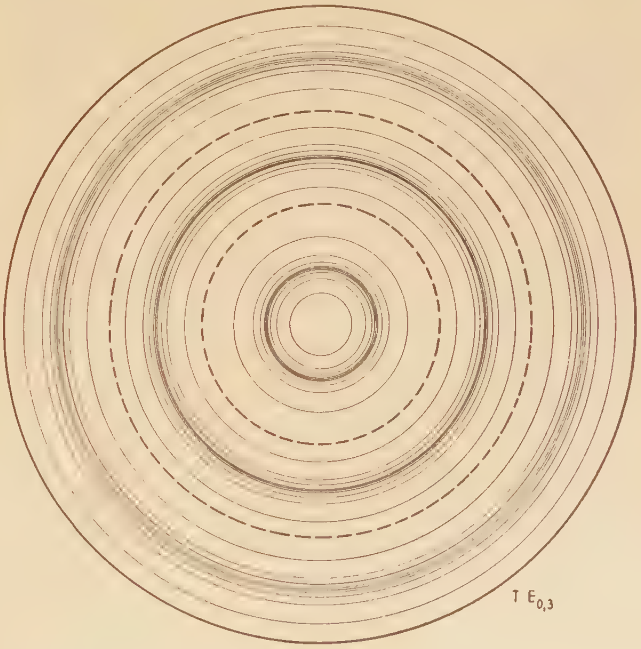


Figure 27. Circular magnetic waves ($\mathrm{TM}_{0,M}$) in metal pipes of circular cross section

The solid lines are magnetic lines of force. The projections of electric lines of force on a transverse cross section are perpendicular to the magnetic lines. The boundary and the dotted circles are lines of zero electric potential, and, therefore, perfectly conducting cylindrical partitions, coaxial with the pipe and passing through the dotted circles, can be inserted without disturbing the field. The number of such partitions is M. The radii of the nodal circles are approximately proportional to 2.40, 5.52, 8.65, 11.79, 14.93, 18.07, . . ., K_α, $K_\alpha+\pi$, . . .

Figure 28. Circular electric waves ($\mathrm{TE}_{0,M}$) in metal pipes of circular cross section

The solid lines are electric lines of force. The projections of the magnetic lines of force on a transverse cross section are perpendicular to the electric lines. The boundary and the dotted circles are lines of zero electric field, and, therefore, perfectly conducting cylindrical partitions, coaxial with the pipe and passing through the dotted circles, can be inserted without disturbing the field. The number of such partitions (including the boundary of the guide) is M. The radii of the nodal circles are approximately proportional to 3.83, 7.02, 10.17, 13.32, 16.47, 19.62, 22.76, 25.90, . . ., K_α, $K_\alpha+\pi$, . .



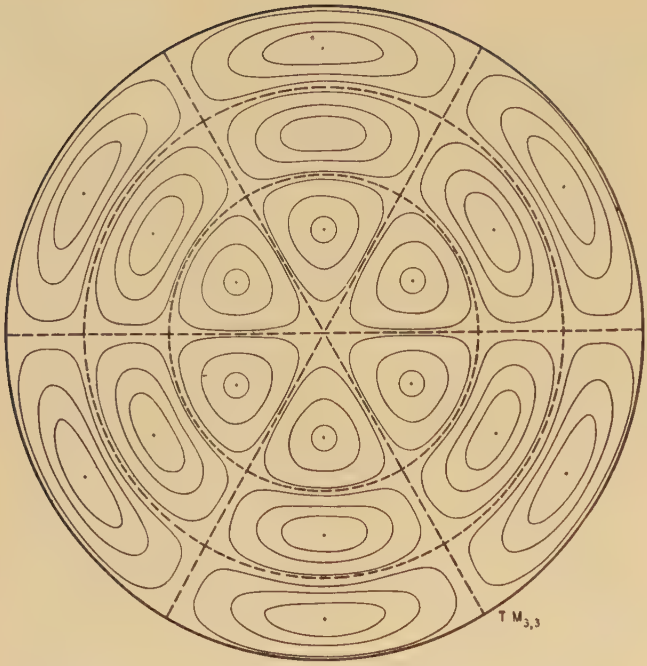
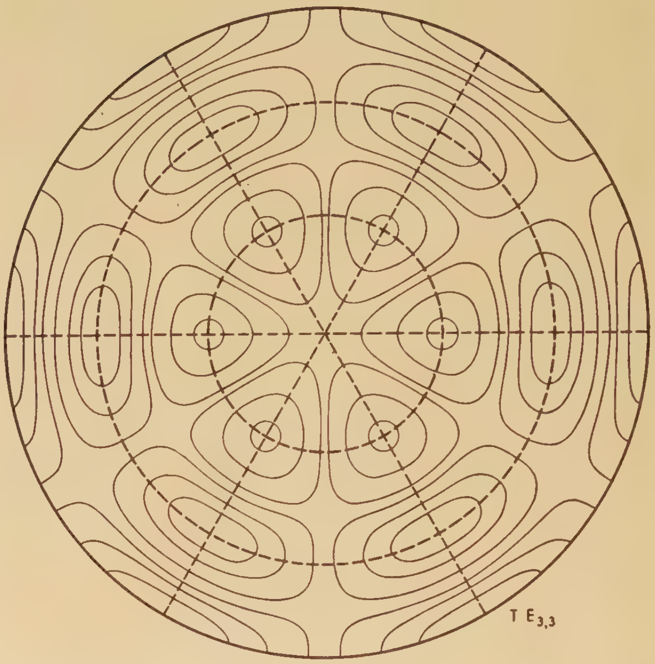


Figure 29. Transverse magnetic sectorial waves (TM$_{L,M}$)

The solid lines are magnetic lines of force. The projections of electric lines of force on a transverse cross section are perpendicular to the magnetic lines. The boundary and the dotted lines are lines of zero electric potential, and, therefore, perfectly conducting cylindrical partitions passing through these lines whose generators are parallel to the axis of the pipe, can be inserted without disturbing the field. Among the above nodal lines there are L diameters and M circles

Figure 30. Transverse electric sectorial waves (TE$_{L,M}$)

The solid lines are electric lines of force. The projections of magnetic lines of force on a transverse cross section are perpendicular to the electric lines. The component of the electric force tangential to the boundary and to the dotted lines is zero, and, therefore, perfectly conducting cylindrical partitions passing through these lines whose generators are parallel to the axis of the pipe, can be inserted without disturbing the field. Among the above nodal curves there are L diameters and M circles



in which the effect of the voltage variation along the line is taken into consideration by assuming that the distribution is sinusoidal (as would be the case if the line were uniform). In the preceding formula x is measured from the open end, and in the present case C is proportional to x.

Each cavity resonator possesses an infinite number of oscillation modes. The mode which we have just discussed happens to be the gravest mode for circular cylindrical cavities, provided the height of the cavity does not exceed the diameter $2a$.

For each oscillation mode, the width of the resonance curves is measured by the ratio $Q=f_0/(f_2-f_1)$, where f_0 is the resonant frequency and f_1, f_2 are the frequencies on either side of f_0 at which the amplitude of the oscillations is 71 per cent $(100/\sqrt{2})$ of the maximum. It may be shown that

$$Q=\frac{\omega \times \text{energy stored at resonance}}{\text{power dissipated}} \tag{43}$$

For the gravest mode in a circular cylindrical cavity, the Q is

$$Q=\frac{452}{\mathcal{R}\left(1+\frac{a}{h}\right)}, \qquad h\leq 2a \tag{44}$$

Thus if $a=h$, and $\lambda=1m$, then the Q for a copper cavity is approximately 50,000.

TRANSVERSE LINES OF FORCE IN TUBES OF CIRCULAR CROSS SECTION

Figures 27, 28, 29, and 30 show the transverse lines of force for typical guided waves in tubes of circular cross section. The lines of force which do not remain in transverse cross sections are not easy to draw; one should visualize them as surrounding the transverse lines.

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Training Technical Personnel for War Industry

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MEMBER AIEE

THE programs of the Army and Navy for the selection and training of technical personnel for the armed forces indicate the prospect of a carefully planned and orderly flow of engineering officers into the armed services. War industry is not so fortunate. There is as yet no comparable plan for insuring the sustained and orderly flow, into our civilian economy, of the engineering talent needed to energize production in the industrial plants to which the Army and Navy look for supplies of war material.

Authorities responsible for the prosecution of the war agree that industry must have a fair share of the available supply of engineers if it is to meet the production schedules that the war effort demands. Several proposals for the selection and training of engineers for industry have been considered by the War Manpower Commission, but to date none of these has been approved or adopted by that body, although it is my understanding that the Committee of Nine which allocates colleges to the Army and Navy for specialized training has reserved 25 per cent of the available facilities for the training of engineers for industry.

This third article to appear in "Electrical Engineering" on the problem of producing engineers in wartime nominates the Engineering, Science, and Management War Training Program as a workable plan for securing leaders for the production line. Articles describing the Army Specialized Training Program and the Navy College Training Program which have already been put into operation appeared in the March issue.

Engineering students have been declared by Selective Service to be eligible for consideration by local draft boards for deferment, provided the college in which such students are enrolled certifies that they are competent and that they will be graduated on or before July 1, 1945. Students are increasingly reluctant to ask for such deferment, however, and local Selective Service Boards are likewise less willing to defer college boys when this now means that men with families must be called up in their stead. In view of these trends, it seems unlikely that industry can look forward to any adequate supply of new engineering personnel via the deferment route.

There will be a small number of college men, physically disqualified for military service, who can be trained for industry, and possibly more young women can be induced to enter engineering schools. Frankly, I am not very hopeful that large numbers of girls can be persuaded to undertake long-term curricula in engineering, because the evidence to date indicates that, whereas women are anxious to help in the war effort and will take quick training courses to prepare themselves for immediate service in industry, most of them are not interested in engineering as a career and do not want to undertake a full engineering education.

Unless the War Manpower Commission evolves some method whereby engineering students can be prepared for industry in the colleges on some plan similar to the Army and Navy specialized training programs, it is clear that our civilian economy will soon be very largely cut off from its normal sources of recruiting engineering talent. In view of the obvious difficulties in selecting equitably and training a quota of able-bodied young men specifically for civilian purposes, at a time when all other qualified young men are going into military service, I think it is unlikely that a feasible plan will be found.

If a sufficient supply of young engineers cannot be expected from the colleges, what then is the War Manpower Commission prepared to do for industry, and how is industry to solve its pressing problem of obtaining technical personnel? A program that already has helped a great deal and that can help a great deal more in the months ahead is the Engineering, Science, and Management War Training Program (ESMWT for short).

Since the supply of fully qualified engineers for industry is already small and likely to be even smaller, in the absence of any adequate flow of new blood from the engineering schools, it is clear that we must utilize at maximum efficiency the engineers still available, by providing them with assistants who can relieve them of as much routine work as possible. Such assistants can be trained under the ESMWT program for the specific tasks that industry wants them to perform, and then they can be upgraded through in-service courses for

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Essential substance of an address delivered at the AIEE Northeastern District technical meeting, Pittsfield, Mass., April 9, 1943.

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larger responsibilities, as rapidly as their abilities warrant and the time available permits.

Let me outline briefly how the ESMWT program operates and what it is prepared to do for industry.

ORIGIN AND PURPOSE

Originally sponsored by the United States Office of Education at the suggestion of a group of leading engineering educators who foresaw $2^1/_2$ years ago that there would be a serious shortage of technically trained people as the defense program developed, Engineering Defense Training, as it was first called, was designed to provide short intensive courses at the college level to fit men and women for technical jobs in war industries. Limited at first to engineering, the program was broadened later to include chemistry, physics, and production supervision, and the title was changed to Engineering, Science, and Management War Training.

ORGANIZATION AND ADMINISTRATION

In a recent reorganization of the War Manpower Commission, ESMWT became one of six components of the Bureau of Training headed by W. W. Charters. The other five agencies—National Youth Administration, Training Within Industry, Vocational Training for War Production Workers, Apprentice Training Service, and Vocational Training for Rural War Production Workers—all are concerned with training at the vocational level.

This year the agencies which comprise the Bureau of Training are spending over $200,000,000 of government funds in war-production training. The $30,000,000 allocated to ESMWT is about the same sum of money that it took to operate all of the engineering schools of the country for one year in the middle 1920's, at the time of the investigation of engineering education conducted by Doctor Wickenden for the Society for the Promotion of Engineering Education.

The actual administration of ESMWT is handled with notable efficiency in the United States Office of Education under the able leadership of George W. Case, dean of the University of New Hampshire's college of technology, by a small staff, most of whom are on leave of absence from engineering faculties. Participating colleges, now over 200 in number and including all of the nation's engineering schools, determine the needs of industry in the areas that they serve, propose appropriate courses to meet these specific needs, and assist in placing the trainees where they can be most useful to the war effort.

Regional co-ordination is provided through 21 part-time regional advisers who serve without compensation and act as chairmen of committees made up of representatives of participating institutions in their areas. The whole program is a spontaneous and voluntary one in which the colleges have co-operated enthusiastically on a nonprofit basis as part of their contribution to the national welfare in this emergency. Institutions are reimbursed for the costs of offering ESMWT courses, and trainees pay no tuition but are expected to purchase their own textbooks and minor supplies.

TYPES OF COURSES OFFERED

The ESMWT program is very flexible and, within the limits of its enabling act, can serve to meet a wide variety of industrial needs. High-school graduation or the equivalent is the minimum requirement for admission to these training courses, but many of them require several years of college work or even a college degree as qualification for enrollment. Institutions are free to set up such eligibility requirements as they feel are necessary to the proper conduct of a course and to select the trainees who can be admitted, provided there is no discrimination because of sex, race, creed, or color. Wherever possible, employers have been asked to co-operate with the colleges in the selection of suitable trainees.

Two patterns of procedure have developed in the offering of ESMWT courses. In some communities representatives of the colleges have been able to secure the interest and co-operation of war industries in a very intimate working relationship, whereby the planning and offering of these courses has become in effect a joint enterprise. The manufacturers have analyzed carefully their personnel problems, estimated losses likely to occur through Selective Service in various categories, studied the particular knowledges and skills that would be needed by replacements, worked out tailor-made courses of study with members of the college faculty, assisted in the selection of trainees who would be employable after training, and finally placed the people thus trained in the company's employ.

Many times the course is taught by a specialist from industry rather than by a college teacher, and the instruction may be even given off campus, at the plant or in rented areas conveniently near it. While the courses offered are all of college grade, they are not traditional units of engineering curricula but are individually designed to meet the specific requirements of definite jobs that industry needs to fill. The plan is flexible enough to provide for training ranging from basic mathematics and mechanical drawing at one extreme to advanced mechanics, X-Ray-diffraction analysis of metals, geometrical optics, and ultrahigh-frequency techniques at the other.

It is not always possible to obtain in ESMWT courses the ideal setup, in which the trainee's entire program is preplanned, although this is the objective toward which we are striving constantly. Industrial executives are sometimes too busy to give the necessary time for such a program, frequently they can't anticipate their personnel needs adequately, and the smaller companies particularly do not have sufficient staff to warrant running special courses for their own plants alone.

To fill their needs and to provide a pool of trained workers in fields where known shortages exist, the colleges have offered open courses in engineering drawing, motion and time study, production control, industrial safety, metallography, mathematics, tool design, electrical inspection and testing, and similar fields, and have publicized these rather widely, with the object of serving people whose value to war production would be enhanced through such training and who would not be reached otherwise. The results of such courses have been very gratifying in that large numbers of the trainees have been upgraded into more responsible jobs in the plants where they were employed or have been moved from nonessential activities into war-production jobs. Some 12,000 short courses of college grade will be offered under ESMWT this year, enrolling over 600,000 trainees.

PRE-EMPLOYMENT COURSES

Large numbers of women are being inducted into industry, and the competition for this new labor supply is extremely keen. A natural tendency of women who want to do war work, even well-educated women, will be to accept jobs immediately available or available after a very brief paid-while-you-learn training course designed to provide them with the elementary skills needed by machine operators in the production line. Such women have no desire to remain in the labor market after the war and thus have no personal advantage to gain in preparing themselves for technical service at the professional or semiprofessional level.

They can be appealed to successfully, however, to undertake such preparation as a patriotic duty, provided we make it possible for them to do so without too great a financial sacrifice. This means that industry must help in the initial selection of such trainees, must put them on its pay roll as soon as training begins, and must provide opportunity for them to advance in responsibility and in earning power through in-service training after the pre-employment program has been completed. Only through such planning will industry obtain for technical service the capable women it needs, who otherwise will tend to be absorbed in routine jobs far below their abilities.

New England industry has been very slow to recognize this problem. In many other sections of the country, particularly on the west coast, full-time ESMWT courses are being given to women trainees who are paid by the companies for which they are being trained. The courses range from 6 to 16 weeks in length and include training in mathematics, physics, production processes, drawing, and elementary engineering mechanics. Sufficient training is given in the pre-employment course to make the trainee initially useful to the company. Obviously a program of supplementary training is necessary for the further development and continuous upgrading of the new worker.

An interesting example of pre-employment training of women under ESMWT is the program currently being operated for the Army Map Service. Several hundred senior girls in women's colleges are taking an orientation course in military map making in preparation for after-graduation jobs with the Army Map Service in Washington. The course comprises an introduction to planimetric and topographic maps, map drafting, projections, military grids and map series, map-reproduction methods and photomapping in two dimensions. Since the course is only 60 hours in length and is studied, in addition to a full college load, it obviously gives the girls only a start in their new work. Further in-service training is being planned for these girls after they are placed on the job.

SUPPLEMENTARY OR IN-SERVICE COURSES

By far the largest amount of ESMWT training has been of the part-time supplementary type. This will probably continue to be true in the months to come, notwithstanding the increased number of pre-employment courses for women that are anticipated. The only way in which industry can hope to obtain the technical personnel that it must have to implement the war-production program is through a continuous process of upgrading. People with potential ability to handle work above the level of that which they are doing must be encouraged to acquire new knowledge and skills through in-service training. The colleges participating in ESMWT will co-operate gladly in organizing and supervising the appropriate courses, but they will need the guidance of industrial executives in laying out the subject-matter content and in selecting the trainees most likely to become more useful in war production as a result of the training.

A specific example may be of interest by way of illustration. One of our ESMWT representatives at Northeastern University discovered that several members of the engineering staff of a large radio-manufacturing company were very much in need of a course in ultra-high-frequency techniques. The head of our electrical-engineering department, in conference with the chief engineer of the company, worked out a combined lecture and laboratory course to meet three evenings a week for 16 weeks. Other radio manufacturers in the area were circularized about the prospective course, and many of them designated employees whom they wished to have enrolled. Incidentally, the course was limited to college graduates who had majored in electrical engineering or physics, and the trainees are expected, not to get new jobs as a result of the course, but to be more effective in their present positions.

An executive of an electrical manufacturing company that was experiencing difficulty in certain phases of radio-tube production suggested a course in glass processing. Such a course was developed and publicized by word of mouth only, but the demand for it was so great that four separate sections of the course were organized in various suburban areas of Greater Boston. This is the type of need that ESMWT is prepared to meet.

CONCLUSIONS

ESMWT cannot and does not attempt to produce full-fledged engineering graduates capable of general professional service. Perhaps the War Manpower Commission will find a way of channeling a reasonable proportion of young fully trained engineers into war industry. Let us hope so. In the meantime our colleges and engineering schools stand ready to bridge the gap as effectively as possible through the facilities of the ESMWT program.

To summarize briefly what ESMWT is prepared to do and how industry can help if it wants such training programs:

1. The colleges participating in ESMWT can organize and offer, without cost to industry and without tuition charge to the trainees, short courses ranging in length from 6 to 16 weeks in the fields of engineering, physics, chemistry, or production supervision (cost accounting, statistics, traffic management, personnel administration, industrial management, procurement and stores, office management) to meet specific training needs.

The courses can be of the full-time pre-employment type or of the part-time supplementary type, can be given day or evening, on campus or off campus at a more convenient location for those enrolled, can be of an elementary or advanced nature, as long as the subject matter is at the college level and the trainees have at least a formal high-school education or the equivalent, and can be taught by a member of the college faculty or by a teacher selected from industry.

There is no desire on the part of the colleges to promote ESMWT courses just for the sake of building up a program. College faculties, engineering faculties especially, are going to be very busy with Army and Navy training programs, and they have no desire to add unnecessarily to their burdens. At the same time they are ready and eager to serve industry as fully as industry wants to be served in the provision of technical training.

2. Engineers who are in the key positions in industry, therefore, have the responsibility of indicating to us as clearly as possible their particular needs for trained personnel. We must depend upon them to advise us on course content and to aid us in the selection of the trainees who will be most serviceable in increasing production after they are trained. And finally, industry must make provision for paying women trainees while they are taking ESMWT full-time pre-employment courses, if such women are to be attracted to college-level technical training.

Electric Conveyances in Air-Foam Rubber Production

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THE manufacture of sponge-rubber forms involves the creation of bubbles inside the rubber and the necessity of maintaining their even distribution. Such a process presents the problem of handling the material very carefully at all times and with sufficient regularity and rapidity to permit the process to be performed in a production-line manner. Such a problem is posed by the material-handling system recently installed at the Akron (Ohio) plant of the Goodyear Tire and Rubber Company. The solution embodies a combination of squirrel-cage motors with dynamic braking and d-c adjustable-voltage drive with Rototrol control all closely tied together with control interlocking and actuated by numerous limit switches.

Air-foam rubber is manufactured from latex, which is mixed with other necessary ingredients to form a doughy mass. This is placed in tight molds and cured in vulcanizing ovens. This curing process must be performed under accurate time and temperature control. Upon removal from the mold, the product is complete.

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Special handling problems have arisen in the manufacture of sponge-rubber forms because of the necessity of creating bubbles inside the rubber and maintaining them in even distribution. To cope with the problem of handling the material carefully and at the same time rapidly enough to employ production-line technique, electric equipment involving a combination of squirrel-cage motors with dynamic braking and d-c adjustable-voltage drive has been developed.

MECHANICAL SYSTEM

Briefly, the mechanical equipment involved is as follows. A mixing machine and rotary platform prepare and distribute the "green" mixture. This is put into aluminum molds as they move slowly past the mixer. These molds are carried on steel frames with rollers called pallets, which are propelled along a track by lugs on a chain conveyor. This conveyor shall be referred to as the pouring conveyor. After being filled, the lids of the molds are automatically closed and held tightly shut as the pallets move to the elevator. On the elevator the pallets are stacked two to a tier and seven tiers high. When this total of 14 pallets has been assembled, they are moved by a pusher onto the transfer car, which puts them in a vulcanizing oven.

The vulcanizing complete, the pallets are again placed on the transfer car, which takes them to the "lowerator," a device similar to the elevator in physical appearance but which performs just the reverse function. It takes the stack of 14 pallets and passes them out one at a time onto the stripping conveyor. This conveyor, like the pouring conveyor, is of the chain type with lugs which engage the pallets and push them along a permanent track. On this stripping conveyor, the completed air-foam products are removed from the molds, which are then cleaned and prepared to repeat the above cycle. At the end of the stripping conveyor, the pallets pass onto a cross conveyor which transfers them to the pouring conveyor at the approach to the pouring point. This completes the cycle, which is repeated continuously.

THE ELECTRIC SYSTEM

The electric equipment for the various drives is next considered in detail. The mixer and ovens are considered as a part of a manufacturing process rather than material-handling equipment and hence are not within the scope of this article.

A common characteristic of all the motors is the necessity for protection from falling objects and pieces of rubber in the process. To secure this protection, the driving motors are either of dripproof or splashproof construction and are rated on the basis of a 50-degree centigrade rise in continuous operation, in accordance with National Electrical Manufacturers Association standards. The adjustable-voltage motor-generator set for driving the transfer car is of the open-protected construction. Spring-set solenoid-released shoe brakes are used for all drives to serve both a retarding and holding purpose. Open floor-mounted brakes are used.

Pouring Conveyor. The pouring conveyor operates continuously, at constant speed. The load, when under full operation, is, for all practical purposes, constant.

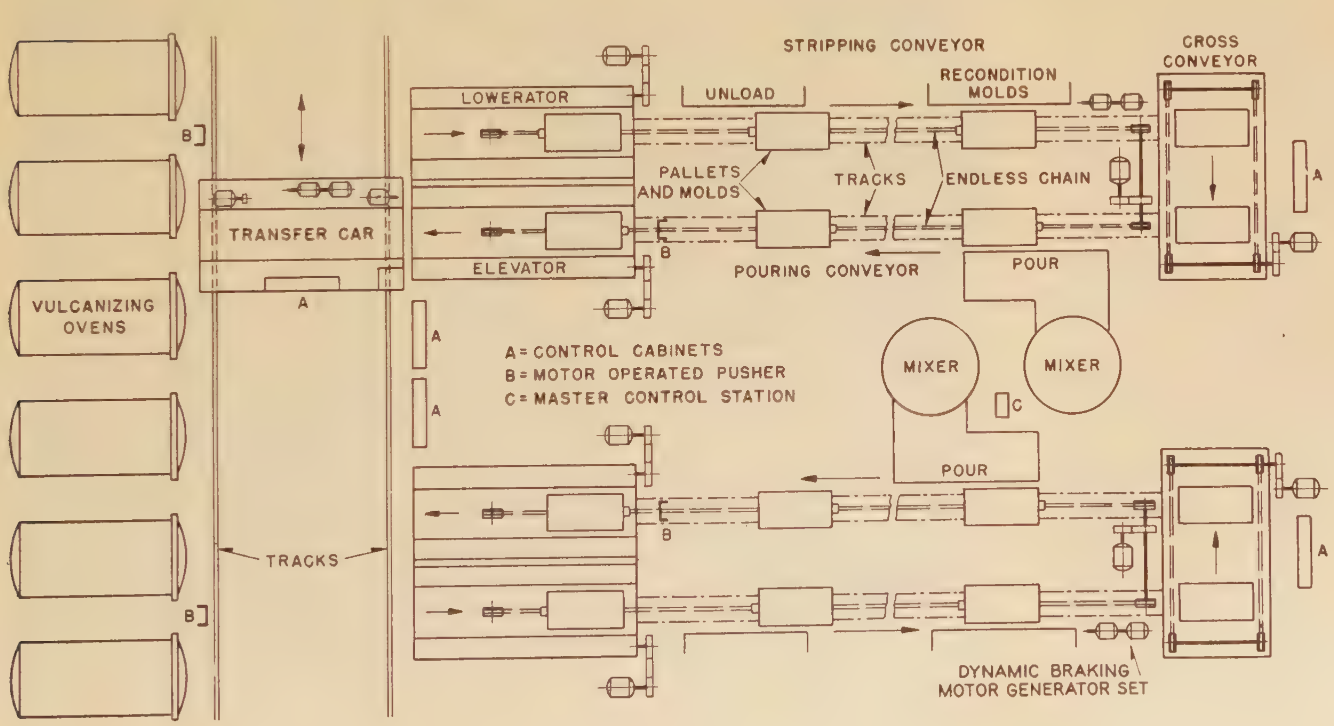


Figure 1. Layout showing relative location of apparatus

With changes in the size of the product, the amount of material placed in the mold will change but because of the basic load of the pallet and mold, the change in load on the drive motor by no means will be proportional. Because of the mechanical construction of the track, antifriction bearings on the pallets, and the use of a chain type of conveyor, the starting friction is reduced from that required on usual conveyor drives. Requirements on the electric equipment are, therefore, for a motor with normal starting torque, uniform speed under all load conditions, and for a motor that can operate continuously under full load. These conditions are met by the 7.5-horsepower general-purpose 1,750-rpm squirrel-cage motor. This motor combines simplicity, strength, and the perfect performance characteristics for the job. The holding brake is rated 50-pound-feet torque continuously.

The control for the pouring conveyor consists of across-the-line starting and d-c dynamic braking for rapid retardation. In this scheme, d-c potential is applied to two of the motor terminals. This sets up a stationary field which induces current in the rotor when it is turned. This current dissipates the energy of rotation as heat in the motor rotor. The equipment involved is a three-pole running contactor, a two-pole dynamic-braking contactor, an overload relay, and a time-limit relay to remove the d-c voltage and set the brake to bring the drive to a final stop.

Control is secured through a main push-button station located at the pouring position. As expected in an involved and interlocked system of this nature, this drive is so interlocked with the elevator that, should the elevator become jammed or fail to operate, the loading conveyor will stop automatically.

Elevator. The drive for the elevator automatically must raise one layer every time two pallets have been placed upon it. Accurate spotting is required to line up the tracks on the elevator with the conveyor and thus reduce the bounce when the pallets pass from the conveyor onto the elevator. This accurate spotting must be achieved under a condition of varying load, which varies from the small friction load of the elevator, gradually increasing to full load as successive pairs of pallets are added. The mechanical construction of the elevator is such that this drive does not have to be reversed.

The requirements of operation under varying loads with approximately the same speed under all conditions from basic friction to maximum is met by the use of a 15-horsepower, squirrel-cage gear motor. This consists of a motor close-coupled to a set of double-reduction gears with 87-rpm output speed, giving in a small compact unit an electric drive with a low output speed. This gear motor is controlled by a nonreversing across-the-line type of starter with which, in order to secure the quick accurate stopping desired and reduce excessive wear on the 25-pound-foot torque brake, d-c dynamic braking is again employed. This, together with the employment of hatchway-type limit switches to initiate starting and stopping, accounts for the precision achieved. The impulse to hoist the elevator is given by hatchway-type limit switches when two pallets have been placed on a given tier. Another limit switch geared to the elevator drive functions when the elevator has been raised one level to stop the hoist motion. Proper limit-switch protection and interlocking is provided so that when the elevator has been filled to capacity, it will not operate until emptied and the loading conveyor will be stopped to prevent more pallets from being forced onto the elevator.

Pusher. A pusher is provided to remove the pallets from the elevator to the transfer car. Because of the necessity of handling the material carefully, the pallets must be pushed slowly and gently. However, in order to prepare for the next loading of the elevator, the pusher

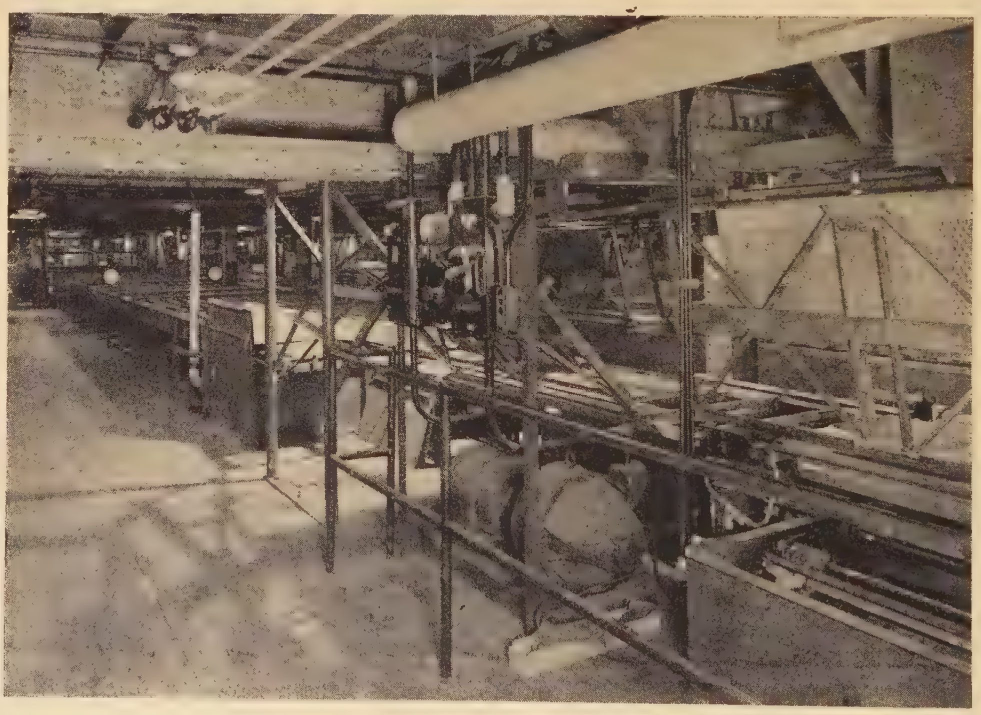


Figure 2. Stripping conveyor discharging empty pallet with open mold onto cross conveyor. Motor-generator set in foreground provides direct current for retarding cross conveyor and elevator motors

must be returned to its original position at a faster speed. This imposes variable-speed reversing service on the pusher drive. Since the function is to push the pallets on their rollers in a horizontal plane normal starting torque is sufficient. This drive is best handled by a 1.5-horsepower constant-horsepower two-speed two-winding 1,150/435-rpm squirrel-cage motor.

The control is of the across-the-line type, the motor being placed on the slow speed winding for pushing, retarded by d-c dynamic braking and returned on the high-speed winding. The transfer-car operator initiates the pusher drive from a push-button station located at the loading position. Interlocking is provided to prevent operation of the pusher unless the transfer car is in the proper position to receive the pallets. Furthermore, a hatchway limit switch automatically reverses the pusher when the pallets have been placed on the transfer car, and another limit switch stops the pusher when it returns to its normal position.

Transfer Car. The transfer car consists of a platform which moves on a track transverse to the pouring and stripping conveyors. To maintain the accuracy of limit-switch settings against slipping wheels, the car is provided with double-flanged wheels riding on a rail and is propelled by a pinion engaging long racks placed on the floor. On this car is located a cage which receives the 14 pallets stacked in the elevator. Upon reaching the oven a "gangplank" is extended from the transfer car to bridge the gap between the car and the oven. The cage containing the pallets is then pushed along the tracks from the transfer car into the oven, the "gangplank" withdrawn, and the car moves on to perform another loading or unloading operation. Driving the transfer car involves cushioned acceleration and extremely accurate spotting of the car under widely varying load conditions. Furthermore, this must be a reversible drive and capable of just as accurate spotting after traveling 2 feet as 20.

To meet these difficult requirements, an adjustable-voltage d-c drive is employed. This consists of driving the car with a 10-horsepower 250-rpm shunt-wound d-c motor. The d-c energy is supplied from a unit-frame motor generator set (25-horsepower squirrel-cage motor, 10-kw adjustable-voltage d-c generator, 2.5-kw 230-volt d-c exciter) located on the transfer car. With this type of drive, smooth acceleration is possible, and the characteristic of constant speed for widely varying loads can be readily obtained. To meet the accurate spotting requirement, a Rototrol maintains constant speed when approaching a final stopping point regardless of the load on the car. This Rototrol is an auxiliary rotating generator with especially designed, balanced field arranged in a Wheatstone-bridge circuit to react with great sensitivity to varying speeds of the motor. The Rototrol is driven by a small three-phase squirrel-cage motor and rotates at all times except final shutdown of the car. The Rototrol adjusts the field strength of the generator to effect control on the speed of the motor. The transfer car accelerates automatically under the control of inductive time limit relays, proceeding at full speed to the desired point. On approaching this point, a slow-down limit switch is engaged, and the transfer car is driven at a predetermined slow speed under control of the Rototrol as already described. At this slow speed, it is possible to set the final-stop limit switches and adjust the brake torque so that the car will always stop at the desired position. Slow down and final-stop limit switches are of the hatchway type and mounted on the underside of the car.

Because of the nature of the operation, it is never necessary to operate the transfer car at the same time that the cage or "gangplank" is being moved. This makes possible the use of the adjustable-voltage drive for both of the motions on the transfer car and a reduction in the total amount of electrical equipment required. The

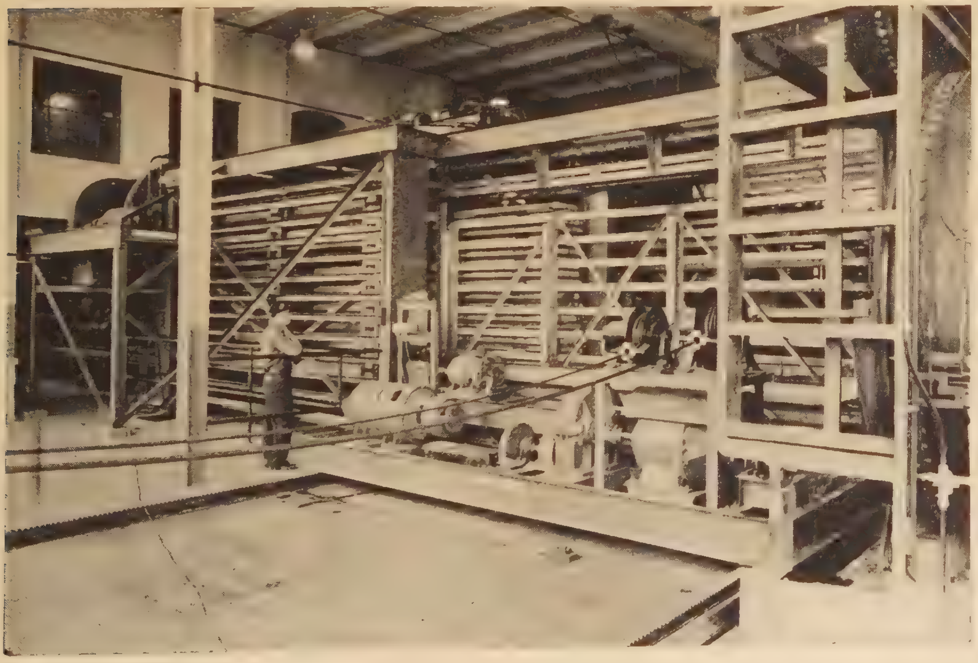


Figure 3. Transfer car and "lowerator" showing construction of each and location of electric equipment on car. Note pusher moving pallets from car to "lowerator"

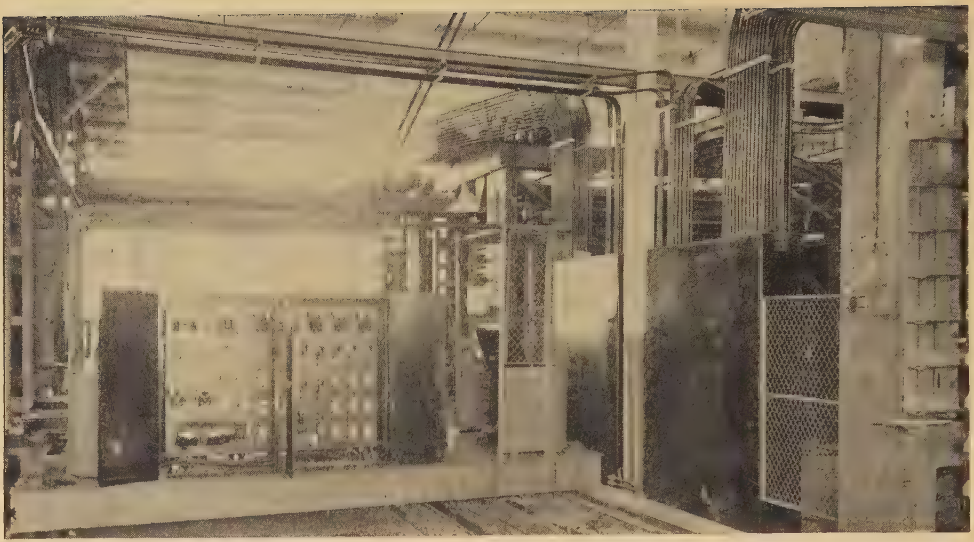


Figure 4. View of transfer car showing Rototrol mounted on control panel and operator's cab

motor generator set is common for both of the drives, individual motors being required for the transfer car drive and the unloading drive. This is spoken of as the unloading drive since a single 5-horsepower 1,150-rpm shunt-wound d-c motor is used first to move the "gangplank" into position and, when this has been performed, to move the cage along the cross tracks into the oven. Adjustable-voltage drive is particularly suitable for this motion also in that the cage must be accelerated smoothly and gently handled.

Since operation of the transfer car cannot be predetermined and assigned any definite routine, and also, since it is advisable to have the operator as close to a delicate operation of this nature as possible, it was decided to have him ride on the transfer car. All of the motions including unloading are automatically controlled with the operator merely selecting, by push button, the oven, loading, or unloading station, to which he desires the transfer car moved. The unloading drive initiated by a push button in the operator's cab is controlled by a traveling nut limit switch, which determines the location of the accelerating and retarding points, in addition to the final stopping points.

The drives for the "lowerator" and its associated pusher, are duplicates of those for the elevator and its pusher, with the exception of such requirements as to perform just the opposite duty. The "lowerator" and both pusher control panels are located with the elevator panel at the vulcanizing end of the conveyor. The details of these drives are so similar that it is not necessary to describe them further.

The stripping conveyor is directly coupled to the pouring conveyor and operated by the same motor and speed reducer. This drive has already been discussed.

Cross Conveyor. At the opposite end from the ovens, a cross conveyor is used to transfer the pallets from the stripping to the pouring conveyor. The requirement is that the cross conveyor must line up a section of track with the track on the stripping conveyor so that the pallets may be placed upon it; then, having completed the travel, it must line this pallet up in such a position that it will be transferred onto the track of the pouring conveyor. This calls for accurate stopping of a nonreversing drive. This necessitated the use of d-c dynamic braking in a manner similar to that in which it was used on the elevator and "lowerator" drives. The conveyor is operated by a 5-horsepower squirrel-cage gear motor, with an output speed of 62 rpm. This motor is started directly across-the-line, and its action is initiated automatically by a hatchway-type limit switch when the pallet arrives on the cross conveyor. It is automatically retarded, and finally stopped by limit-switch action when it has travelled the desired distance.

The direct current for the dynamic braking excitation of the elevator, "lowerator," pushers, loading, stripping, and cross conveyors squirrel-cage motors is supplied by a single 15-kw 125-volt 1,750-rpm a-c to d-c motor generator set driven by a squirrel-cage motor. This motor generator set is started across-the-line, provided with overload protection and sufficient metering and voltage-adjusting equipment to maintain a proper d-c voltage. An interlocking relay is provided to prevent the operation of any of the associated drives, unless the source of the direct current is available for the dynamic braking. The control panels for the cross conveyor, main conveyors, and braking d-c motor generator sets are located at the pouring end of the main conveyors.

In general, the two main conveyors are controlled from the main push-button station at the loading position. The elevator and "lowerator" operate automatically as does also the cross conveyor. The pusher both for the "lowerator" and elevator are operated from push buttons located within reach of the transfer-car operator. All of these associated drives are, of necessity, interlocked to prevent any jamming or destruction of material, pallets, or conveying equipment. Numerous emergency stop buttons are located about the stripping positions.

Completely co-ordinated, the individual drives function with clockwork precision as gears in a machine to interchange pallets in a smooth stream from the time they receive the "green" latex until they have discharged their finished air-foam product and are again in a position to receive another charge.

INSTITUTE ACTIVITIES

National Technical Meeting Program Enlarged Again to Aid War Effort

An enlarged technical program of 26 sessions and conferences, as compared with a maximum of 20 sessions and conferences for the previous year, will be held in Cleveland, Ohio, June 21–25. The meeting headquarters will be in the Hotel Cleveland. In accordance with AIEE wartime policies as adopted by the board of directors all programmed social activities have been omitted. However, there will be opportunity for informal get togethers, and members desiring to participate should inquire at the registration desk.

In addition to the 26 sessions and conferences, the conference of officers, delegates, and members will be held both in the morning and afternoon on Monday. The annual meeting will take place Tuesday morning.

TECHNICAL SESSIONS AND CONFERENCES

Many of the sessions and conferences are directly or indirectly related to the war effort. The details of design and special problems in connection with electrical equipment for aircraft will be presented in a session and a conference. Wartime emergency loading of conductors will be the subject of another session. Closely allied to the question of overloading will be still another session, which has to do with transformers. Several conferences will deal with teaching aids in specialized training, experiences and problems in military specialized training, and the teaching of ultrahigh frequency. Also, the part which electricity plays in the industrial-fabrication end of the war effort will be brought out in the session on electronics in industrial power applications and in the conference on resistance welding and its associated power-supply system.

In various sessions eight wartime guides or interim reports, which treat the application and operation of electrical equipment and apparatus under war conditions, will be presented. These wartime guides or interim reports have been developed by AIEE technical committees and subcommittees in collaboration with the standards committee as a wartime emergency measure to conserve critical materials. In addition, the Association of Edison Illuminating Companies subcommittee on wartime temperatures for power cables in service will present a guide on that subject.

There will be a conference on civic affairs and another on the subject of "How Can the Institute Render Greater Service to its Latin American Members?"

INSPECTION TRIPS

Because of wartime restrictions in the matter of transportation and plant protection, only two inspection trips are scheduled. These are directly associated with two of the technical conferences. It is necessary that firm commitments for transportation and plant-protection identification be made in advance, and the committee on transportation and trips requests that advance registrations for these trips should be made by writing J. J. Tesar, in care of Cleveland Electric Illuminating Company, Cleveland, Ohio, chairman, trips and transportation committee. These inspection trips will be limited to United States citizens, and those participating are reminded to have their birth certificates or other proof of citizenship with them. The Ohio Crankshaft Company will be visited, and details relative to induction-heating processes will be shown. Another trip will be to Nela Park, the lighting laboratory of General Electric Company.

ADVANCE REGISTRATION

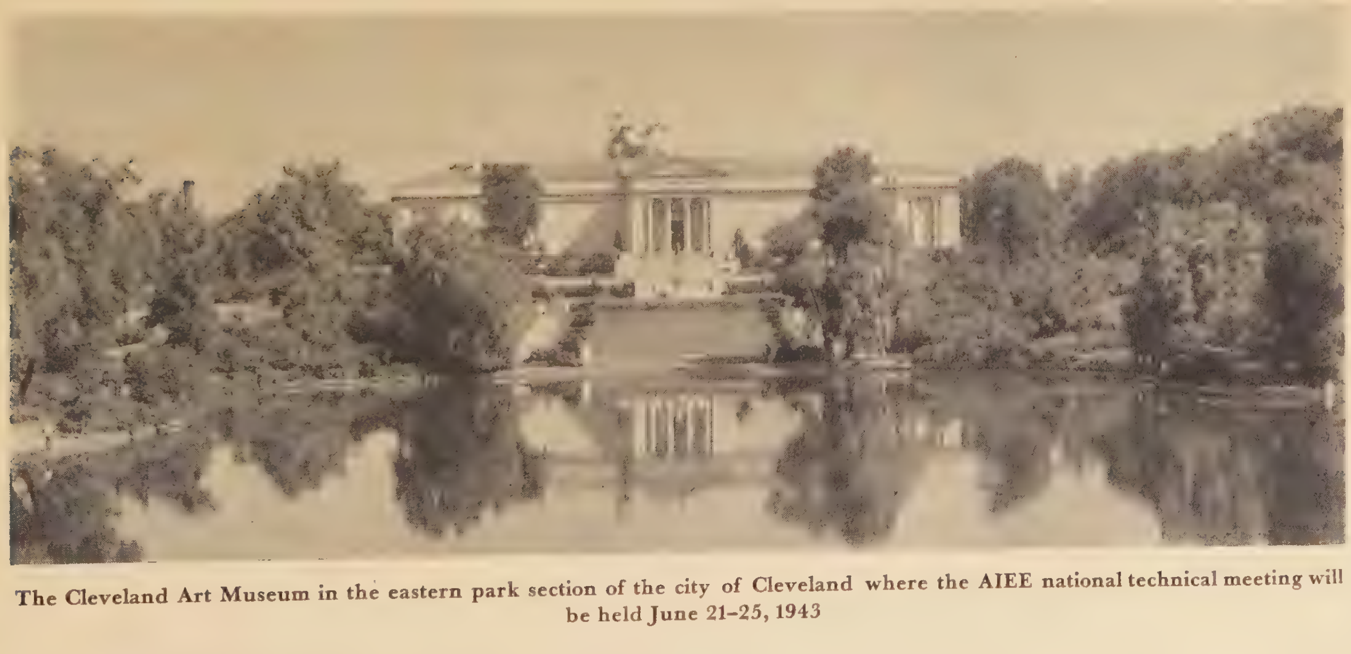
Members who have received an advance registration card should fill in and return it promptly, if they have not already done so. This will permit the registration committee to have badges ready and will prevent congestion at the registration desk. In accordance with regular practice, a registration fee of two dollars will be charged all nonmembers, excepting enrolled students and the immediate families of members.

HOTEL RESERVATIONS

Hotel reservations should be made by writing directly to the hotel preferred. For convenience, a room registration card for the Hotel Cleveland, meeting headquarters, was included with the mailed announcement sent to most members.

COMMITTEES

Members of the national technical meeting committee and chairmen of subcom-



The Cleveland Art Museum in the eastern park section of the city of Cleveland where the AIEE national technical meeting will be held June 21–25, 1943

AIEE National Technical Meeting,

- **ABSTRACTS of papers appear on pages 266–71 of this issue.**
- **PRICES and instructions for securing advance copies of these papers accompany the abstracts. Only numbered papers are available in pamphlet form.**

Monday, June 21

9:00 a.m. Registration

10:00 a.m. Conference of Officers, Delegates, and Members

J. M. Thomson, vice-chairman, sections committee, presiding

Chairman's opening statement

Address, "Impressions Obtained From My Visits to the Sections During the Past Year." **H. S. Osborne**, president

Address, "How the Institute Is Getting Along in Membership and Finances." **H. H. Henline**, national secretary

Report, "What the Sections Committee Has Been Doing in South America." Subcommittee, **O. C. Brill**, chairman; **M. S. Coover**

Address, "How the Mexico Section Is Getting Along"

Address, "I Am an American." **A. Boyajian**, General Electric Company

Report, "Development of Subsections." Subcommittee, **E. W. Schilling**, chairman; **C. A. Faust**

Report, "Other Activities of the Sections Committee During the Past Year." **E. T. Mahood**, chairman, sections committee

2:00 p.m. Clinic on Section Operation and Management of Larger Sections (More Than 300 Members)

G. W. Bower, presiding

Explanation of "clinic" plan and introduction of members of sections committee present

R. M. Pfalzgraff, vice-chairman, program subcommittee

Discussion by delegates

2:00 p.m. Clinic on Section Operation and Management of Smaller Sections (Less Than 200 Members)

W. J. Gilson, vice-president-elect, Canadian District, presiding

Explanation of "clinic" plan and introduction of members of sections committee present

J. M. Thomson, chairman, program subcommittee

Discussion by delegates

Tuesday, June 22

10:00 a.m. Annual Meeting

President Harold S. Osborne, presiding

Address by **H. W. Green**

Board of directors' report (in abstract), **H. H. Henline**, national secretary

National treasurer's report, **W. I. Slichter**

Committee of tellers' report on election of officers

Presentation of president's badge; response from **President-Elect Nevin E. Funk**

Presentation of prizes for papers

Presentation of Lamme medal to **Joseph Slepian**

President's address, **Harold S. Osborne**

2:00 p.m. Conference on Civic Affairs

43-122–ACO. The Engineer's Interest in City Planning. Report of the conference on city and regional planning at the AIEE North Eastern District technical meeting, Pittsfield, Mass., April 8. **F. A. Cowan**, vice-chairman, technical program committee.

Speakers: **F. A. Cowan, H. W. Green, J. T. Howard**

2:00 p.m. Power Generation

43-93–ACO. Interim Report of Emergency Measures to Increase Output of Generating Equipment and Systems. AIEE committee on power generation

CP.** Tentative Specification for Speed Governing of Steam-Turbine Generators Rated Not Less Than 10,000 Kw. Report of Joint AIEE–ASME Committee on a Proposed Specification for Prime-Mover Speed Governing

2:00 p.m. Conference on Resistance Welding and Its Associated Power-Supply System

This will provide an opportunity to present problem arising from the extended use of resistance welders in industry today. Industrial engineers are to be encouraged to present their problems. Speakers will represent the manufacturers of resistance welders and control equipment, public utilities companies, and industrial users. Topics of discussion will be:

CP.* Resistance Welders, Characteristics, Controls, and Auxiliary Equipment

43-121. Automatic Voltage Compensator for Resistance-Welding Control. **E. M. Callender** and **R. S. Phair**, Edward G. Budd Manufacturing Company

CP.* Adequate Power-Supply System for Resistance Welders, with discussion on such factors as low voltage, lighting flickers, low power factor, and load factor; also Various Methods of Correcting Load Characteristics

CP.* Application of Series Capacitors to Resistance Welding Loads

2:00 p.m. Conference on Safety

Any time lost through accident is a war-manpower problem. In this conference emphasis will be on the safety program of the smaller electrical-manufacturing and utility organizations. After presentation of prepared papers ample time will be provided for round-table discussions.

43-94–ACO†. Practical Accident Prevention in Small Groups. **O. S. Hockaday**, Texas Electric Service Company

Wednesday, June 23

10:00 a.m. General Session

2:00 p.m. Arc-Furnace Control and Transients

43-97. A Study of Voltage Transients in Arc-Furnace Circuits. **J. B. Hodtum** and **J. B. Rice**, Allis-Chalmers Manufacturing Company

43-98. Practical Calculation of Electrical Transient on Power Systems. **R. D. Evans** and **R. L. Witzke**, Westinghouse Electric and Manufacturing Company

43-95–ACO. Arc-Furnace Control by Regulex Exciters. **T. B. Montgomery** and **T. H. Bloodworth**, Allis-Chalmers Manufacturing Company

43-96. Rotating Regulator for Arc Furnaces. **R. A. Geiselman, C. C. Levy,** and **W. R. Harris**, Westinghouse Electric and Manufacturing Company

2:00 p.m. Conference on "How Can the Institute Render Greater Service to Its Latin American Members?"

CP.* International Electrical Standards. **L. F. Adams**, General Electric Company

CP.* Relations Between Latin American and United States Standardizing Bodies, With Particular Reference to Electrical Standards. **Alberto Magno-Rodrigues**, American Standards Association

2:00 p.m. Synchronous Machinery

43-76. Pole-Face Loss in Solid-Rotor Turbine Generators. **W. W. Kuyper**, General Electric Company

43-89. The Quadrature Synchronous Reactance of Salient-Pole Synchronous Machines. **R. V. Shepherd** and **C. E. Kilbourne**, General Electric Company

43-99. The Calculation of Unbalanced Magnetic Pull in Synchronous and Induction Motors. **R. C. Robinson**, Westinghouse Electric and Manufacturing Company

2:00 p.m. Conference of Student Branch Counselors

This conference will deal with Student Branch problems, particularly as related to postwar programs.

Thursday, June 24

9:30 a.m. Induction Machinery

43-83. A New Type of Adjustable-Speed Drive for A-C Systems—II. **A. G. Conrad** and **P. F. Ordung**, Yale University, and **S. T. Smith**, Naval Research Laboratory

43-100. Distribution Factors and Pitch Factors of the Harmonics of a Fractional-Slot Winding. **M. M. Liwschitz**, Polytechnic Institute of Brooklyn

43-82. Critical Material Conservation in Induction-Motor Manufacture. **H. M. Hobart**, Consulting Engineer

CP.* Allowable Temperature Rise of Totally Enclosed Fractional-Horsepower Motors

American Standard C50, covering American standard for rotating electrical machinery, has been revised and is being reissued. It will recognize an allowable temperature rise for totally enclosed fractional-horsepower motors five degrees centigrade higher than that recognized for integral-horsepower motors. This difference is of definite concern to a number of design engineers. The purpose of the conference session is to encourage a full discussion of this matter.

9:30 a.m. Switching Equipment and Supervisory Control

43-84. Interim Report on Application and Operation of Automatic-Reclosing Equipment on Stub Feeders. Subcommittee on a-c automatic-reclosing equipment, AIEE committee on automatic stations

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CP: Conference presentation; copies available on request from **M. J. Steinberg, Consolidated Edison Company of New York, Inc., 4 Irving Place, New York, N. Y.

*CP: Conference presentation; no advance copies of papers available; not intended for publication in *Transactions*.

†ACO: Advance copies only available; not intended for publication in *Transactions*.

Cleveland, June 21-25, 1943

43-101. Interim Report on Characteristics and Performance of Conductors for Supervisory Control and Telemetering. Subcommittee on characteristics and performance of conductors for supervisory control and telemetering, AIEE committee on automatic stations

43-102. The Cause and Control of Some Types of Switching Surges. **T. W. Schroeder,** General Electric Company

43-86. Experience With Oil-Circuit Reclosers on REA Systems. **L. M. Moore** and **B. O. Watkins,** Rural Electrification Administration

43-103. A New High-Interrupting-Capacity Fuse for Voltages through 138 Kv. **H. H. Fahnoe,** Westinghouse Electric and Manufacturing Company

9:30 a.m. Conference on Agricultural Electrification

CP.* The Farmer and His Electricity. **Grover C. Neff,** president, Wisconsin Power and Light Company

CP.* Evolution in Agriculture. **Frank E. Watts,** Farm Journal and Farmers' Wife

CP.* Application Engineering—The Key to Increased Farm Production. **George W. Kable,** editor, *Electricity on the Farm.*

CP.* Progress in Serving Oklahoma Farms. **Roy E. Hayman,** Oklahoma Gas and Electric Company

CP.* Chemurgy—Its Place in This War and After It. **L. F. Livingston,** E. I. duPont de Nemours and Company

Alternate subject (time permitting)

CP.* The Care and Maintenance of Electric Farm Equipment. **A. H. Hemker,** General Electric Company

9:30 a.m. Conference on Teaching Aids in Specialized Training

9:30 a.m. Conference on Induction Heating

Speakers: **H. B. Osborn, Jr., P. D. Zottu, W. E. Benninghoff, J. P. Jordan**

1:30 p.m. Conference on Experiences and Problems in Military Specialized Training

CP.* Safety Engineering Instruction at Oklahoma A and M College. **A. Naeter,** Oklahoma Agricultural and Mechanical College

2:00 p.m. Wartime Emergency Loading of Conductors

43-85. Safe Ratings for Overhead Line Conductors. **L. M. Olmsted,** Duquesne Light Company

43-104. Guide for Wartime Conductor Temperatures for Power Cables in Service. Association of Edison Illuminating Companies subcommittee on wartime temperatures for power cables in service

43-105. Interim Report—Emergency Overloads on Overhead Conductors. Working group on towers, poles, and conductors, transmission subcommittee, AIEE committee on power transmission and distribution

43-77. The Effect of Corona on Coupling Factors Between Ground Wires and Phase Conductors. **G. D. McCann,** Westinghouse Electric and Manufacturing Company

2:00 p.m. Selected Subjects

43.120. Subcenter Switching Systems for Teleprinter Tie Lines. **F. L. Currie,** Western Union Telegraph Company

43-81. A Capacitance Bridge for Determining the Ratio and Phase Angle of Potential Transformers. **H. W. Bousman** and **R. L. Ten Broeck,** General Electric Company

43-106. Formulas for Calculating Short-Circuit Forces Between Conductors of Structural Shape. **T. J. Higgins,** Illinois Institute of Technology

43-107. A Method for Determining the Normal Modes of Foster's Reactance Networks. (Presentation by title) **W. R. LePage,** Radio Corporation of America

43-108. A Method for Correlating Duty-Cycle Tests on Solenoids. **J. E. Ryan,** General Electric Company

43-78. Adequate Electrical Maintenance Essential to Transportation. **W. J. Clardy,** Westinghouse Electric and Manufacturing Company

2:00 p.m. Conference on Wartime Influences in Lighting

3:30 p.m. Conference on Teaching of Ultrahigh Frequency

Speakers: **J. G. Brainerd, H. J. Reich, W. C. Osterbrock**

7:00 p.m. Dinner—Jointly with the Cleveland Section, Institute of Radio Engineers

Address: "Frequency Modulation Versus Amplitude Modulation at High Frequencies." **Raymond Guy,** Radio Facilities Engineer, National Broadcasting Company

Friday, June 25

9:30 a.m. Carrier Current and Miscellaneous Subjects

43-80. Application of Carrier to Power Lines. **F. M. Rives,** General Electric Company

43-91. Carrier-Current Differential Protection for Transformer Banks. **T. A. Cramer,** General Electric Company

43-110. A Simple Method for the Determination of Bushing-Current-Transformer Characteristics. **S. D. Moreton,** General Electric Company

43-111. Report on Application of Lightning-Protective Devices in Wartime. Lightning arrester subcommittee, AIEE committee on protective devices

43-112. Frequency Modulation for Power-Line Carrier Current. **E. W. Kenefake,** General Electric Company

9:30 a.m. Electrical Equipment for Military Aircraft

43-123. Aircraft Electric-Power Supply System. **J. E. Yarmack,** International Telephone and Radio Manufacturing Corporation

43-124. Characteristics and Applications of the General Electric Company Selenium-Rectifier Cells. **E. A. Harty,** General Electric Company

43-125. Fundamental Principles of Amplidyne Applications. **F. E. Crever,** General Electric Company

43-126. Design Relationships for D-C Generators for Use in Aircraft. **S. R. Bergman,** General Electric Company

43-127. Aircraft Inverter Construction. **C. T. Button,** Holtzer-Cabot Electric Company

9:30 a.m. Electronics in Industrial Power Applications

43-113. The Testing of Mercury-Arc Rectifiers. **H. L. Kellogg** and **C. C. Herskind,** General Electric Company

43-90. Theory of Rectifier—D-C Motor Drive. **E. H. Vedder** and **K. P. Puchlowski,** Westinghouse Electric and Manufacturing Company

43-114. Operation of Nonsalient Pole-Type Generators Supplying a Rectifier Load. **M. D. Ross** and **J. W. Batchelor,** Westinghouse Electric and Manufacturing Company

43-115. Load Pickup by a Group of Ignitron Rectifiers. **L. W. Morton,** General Electric Company, and **D. I. Bohn,** Aluminum Company of America

43-116. Thyratron Motor Control. **E. E. Moyer** and **H. L. Palmer,** General Electric Company

9:30 a.m. Transformers

43-88. High-Voltage Power-Transformer Design. **M. B. Mallett,** English Electric Company of Canada, Ltd.

43-87. Interim Report on Overloading Distribution Transformers. Transformer subcommittee, AIEE committee on electrical machinery

43-109. Interim Report on Overloading Current-Limiting Reactors. Transformer subcommittee, AIEE committee on electrical machinery

CP.* Normal and Emergency Loading of Power Transformers. **H. B. Keath** and **W. Godin,** Wagner Electric Corporation

2:00 p.m. Relaying

43-117. A New High-Speed Balanced-Current Relay. **V. N. Stewart,** General Electric Company

43-79. Pilot-Wire Relaying on a Metropolitan System. **T. G. LeClair** and **E. L. Michelson,** Commonwealth Edison Company

43-92. Distance Relay Protection for Subtransmission Lines Made Economical. **L. J. Audlin,** Central New York Power Corporation, and **A. R. van C. Warrington,** General Electric Company

43-118. Relay Protection of Tapped Transmission Lines. **M. A. Bostwick** and **E. L. Harder,** Westinghouse Electric and Manufacturing Company

43-119. Interim Report on Application and Operation of Out-of-Step Protection. Working group of relay subcommittee, AIEE committee on Protective Devices

2:00 p.m. Electrical Equipment for Military Aircraft

43-128. Aircraft Circuit Breakers. **C. W. Kuhn,** Cutler-Hammer, Inc.

43-129. Principles of Aircraft Electric-Motor Protection. **V. G. Vaughan,** Spencer Thermostat Company

43-130. Manual Switches for Aircraft. **R. A. Millermaster,** Cutler-Hammer, Inc.

43-131. Aircraft Contactors. **F. J. Russell** and **A. P. Charbonneau,** Cutler-Hammer, Inc.

43-132. Radio Noise Filters Applied to Aircraft. **C. W. Frick** and **S. W. Zimmerman,** General Electric Company

2:00 p.m. Conference on Operating Experiences With Mercury-Arc Rectifiers

Subject Matter:

(*a*). Industrial application of rectifiers

(*b*). Large rectifier stations

(*c*). Rectifier cooling systems

(*d*). Rectifier harmonics

mittees making arrangements are: W. E. Wickenden, *chairman;* G. R. Canning, *vice-chairman;* B. W. David, *secretary-treasurer;* D. Ramadanoff (secretary-treasurer, Cleveland Section). Subcommittee chairmen: L. T. Blaisdell, *finance;* E. A. Rossrucker, *publicity;* J. C. Strasbourger, *hotels;* A. J. Kres, *registration and information;* P. L. Hoover, *meetings and papers;* J. J. Tesar, *transportation and trips.*

AIEE Prepares Wartime Guides for Electrical Apparatus

A major AIEE activity at the present time is the preparation of wartime guides and reports covering the technical phases of the application and operation of electrical apparatus. This is in accordance with the decision of the board of directors that the Institute should devote its services in every way possible toward aiding directly the war effort.

A number of these Guides and Reports will be presented during the week of June 21 at the national technical meeting in Cleveland along with the general theme of wartime problems and conservation of materials. Consequently, it seems well to review briefly their history, purpose, and status.

Soon after the outbreak of hostilities the Institute recognized that the war emergency would require that maximum possible use be made of all existing equipment and that a minimum amount of critical materials would be available for use in the manufacture of new equipment. This situation emphasized the great importance of nationwide usage of industry standards and standard apparatus. Since the Institute's membership is very broad, and its committees are composed of representatives of users, manufacturers, educators, and others, it is particularly well fitted as an established standardizing body to handle for the industry at large such technical matters as preparation of war-emergency standards or technical-application guides as may be required for "duration" use.

For many years the Institute has promulgated the development of electrical standards and test codes, both directly and through the American Standards Association. Experience has shown that the Institute's standardizing procedure is direct and expeditious, oftentimes resulting in the completion within a relatively short time of standards or reports on standards which served the industry and were later given consideration by ASA in the preparation of an American Standard. These standards have established criteria for electrical-apparatus rating, performance, procedure, and test guarantees. These have been considered pre-eminent throughout the industry for many years. Their preparation has required careful deliberation and study by engineers recognized as authorities in their respective fields. However these standards and test codes are of a fundamental technical nature and do not lend themselves to short-term changes without upsetting seriously design, manufacturing, application, and operating practices. This is an important aspect in considering the formulation of war-emergency standards or guides.

After careful consideration of the wartime needs and the many problems involved in the preparation and use of emergency standards, the standards committee decided that in a great majority of cases the wartime situation could be taken care of effectively by the preparation of a number of wartime guides and interim reports covering the technical application of various types of standard electrical apparatus. In a few instances emergency substitutions or modifications of present standards may be required. Therefore the standards committee in collaboration with the technical program committee recommended that the Institute's technical committees actively proceed with this work.

This wartime basis required many new concepts in the selection, application, and operation of electrical apparatus and equipment in order to take full advantage of all favorable factors, such as ambient temperature and other service conditions, conservative design margins, load factors, use factors, supplemental cooling, spare equipment.

The technical committees of the Institute have been engaged very actively in this undertaking for about a year and have produced a vast amount of data of direct practical value to both manufacturers and users—especially to the war industries. These reports and guides are intended to promote the fullest use of sound engineering principles in the design, application, and operation of the various types of electrical apparatus. Although the impetus for their preparation is coming from the present war emergency and the necessity to conserve critical materials, nevertheless it is recognized that since they are being prepared on a sound engineering basis, much of the work may be of use even after the war.

The guides and reports are intended to present a general consensus of typical American engineering practice in the selection and co-ordination of equipment for various types of electric-power systems under the conditions prevailing during the present war emergency. They also present information on the performance of electrical equipment under operating service conditions which differ from those usually considered as "normal." Thus they indicate the broad adaptability of *standard* apparatus to special conditions as well as the more usual applications. The economic importance of this should be clearly evident with respect to broader usage of specific apparatus and the ultimate reduction in manufacturing costs, delivery time, and installation expense.

Experience has indicated that this work is resulting in the more widespread use of standard apparatus for all kinds of applications and in many instances the heavier loading of existing apparatus such as generators, breakers, transformers, reactors, and cables. All of this leads to very appreciable savings of critical materials and oftentimes eliminates the necessity for construction changes.

Users of electrical apparatus should not feel that in the past they have been too conservative in their applications, as the guides also call attention to the penalty (in terms of shortened life expectancy) which may be incurred by the operation of apparatus beyond its specified nameplate rating.

Each of the guides or reports is to carry a foreword explaining its purpose, and stating that ". . . it has been prepared by a technical committee for the purpose of making essential information immediately available to war industries, thus furthering the conservation of valuable materials and labor for the war emergency, that the information is educational in character and in no way mandatory, and that at the present time it is not intended as a 'standard' and has not yet been formally approved by the standards committee nor by the board of directors."

The criterion upon which they have been accepted is that they are timely and have been prepared for the information of manufacturers and users for the "duration." After the war emergency they will be re-

Subject	Paper Number	Price by Mail (Cents)	Published in "Transactions"
Shunt capacitors	43-48	30	May 1943, p. 220
Power cables	43-104	*	
Circuit breakers and switchgear	43-69	15	†July 1943
Generating equipment and systems	43-93	20	
System grounding impedance	43-5	15	Jan. 1943, p. 17
Industrial-power distribution systems	43-40	70	
Electric motors and motor controllers	43-62	15	
Automatic-reclosing equipment on stub feeders	43-84	20	
Power transformers and regulators	42-156	15	Sept. 1942, p. 692
Distribution transformers	43-87	15	
Current-limiting reactors	43-109	*	
Lightning arresters	43-111	*	
Conductors for supervisory and telemetering	43-101	*	
Out-of-step relay protection	43-119	*	

* Not yet available.

† Tentative schedule.

considered by the standards committee and the committees which prepared them and then will be approved, revised for normal use, or rescinded. Early availability of the guides is a vital necessity in order that they may be of the most value in the war effort. The technical committees have been urged to present these guides and reports at Institute meetings at the earliest opportunity. After presentation and discussion, they will be given further consideration and review.

One war-emergency standard has been prepared—(War Emergency Modification of AIEE Standard 45, "Recommended Practice for Electrical Installations on Shipboard"). However, the guide and report procedure is being followed in other cases in preference to the preparation of special war-emergency standards which might involve extensive redesigning and drastic changes in manufacturing and application practices at this time. It is believed that the guides accomplish the maximum conservation of critical materials, since they provide for the greatest use of existing equipment and systems, as well as new equipment, without changing the fundamental basis on which the present standards have been prepared.

At the present time wartime guides or interim reports on the subjects listed in the table, page 258, are available or will be completed in time for presentation at the national technical meeting to be held in Cleveland in June. Copies of those available in pamphlet form may be obtained by Institute members from AIEE headquarters by referring to the paper number as listed in the table.

Successful Wartime Meeting Held by AIEE South West District

Despite difficulties imposed by wartime conditions, a highly successful technical meeting was held by the AIEE South West District, April 28–30, at Kansas City, Mo. Concurrently the annual engineering conference of the Missouri Valley Electric Association was held at the same headquarters hotel, the Continental. As many of those who attended the combined meetings would have attended both if they had been held separately, the result was a great saving in time, expense, and the use of critical transportation facilities. The two groups participated jointly in several features, notably two general sessions, two luncheons, and two dinners. The AIEE Student Branches within the District also held their annual conference during that period and participated in some of the events of the meeting in addition to carrying out their own program.

Four technical sessions on a variety of subjects constituted the technical program of the meeting. Throughout these sessions, the emphasis was on problems directly related to war activities. Simultaneously, technical sessions were held by the MVEA, and members of both organizations attended all sessions.

Other features of the meeting included a round-table conference on Section operations and management and a meeting of the District executive committee. Details of the principal events are reported in separate items.

Total registration for both groups was 572, of which 338 were AIEE registrants. Detailed registration figures are given in the accompanying tabulation.

OPENING JOINT SESSION

Addresses by the presidents of both organizations were features of the joint general session which marked the official opening of the meeting. J. G. Thorpe, general chairman of the MVEA engineering group, presided.

In the absence of City Manager L. P. Cookingham of Kansas City, Elmer S. Pierson, president of the Vendo Company, delivered the opening address. He declared that the old idea of the West being predominantly agricultural no longer holds, especially now, and he characterized the expanded wartime manufacturing activities of the Middle West as "staggering to the imagination." He predicted that many midwest war plants will remain after the war and cited as one favorable factor the high proportion of native-born labor available in that area. He urged that special provisions be made to educate after the war the men who are now in the armed forces. These men would supply the creative thinking needed for the postwar development of those industries. This creative thinking, he said, is the only element needed, as the Middle West has available plenty of facilities and materials.

What the AIEE is doing to aid the war effort was outlined by E. T. Mahood, AIEE vice-president representing the South West District. He first described briefly the objectives of AIEE and of the local, District, and national meetings held by the respective groups within the Institute. Meeting programs now are geared to war problems, and many joint meetings with other groups are being held. One of the most important technical activities he mentioned was the development of wartime guides for the application of electrical equipment, now being prepared by AIEE technical committees under sponsorship of the AIEE standards committee (*page 258*). He said that many requests for assistance had been received from various government offices, and he cited the war-production clinics now being sponsored jointly by AIEE Sections and other local groups as an aid to the war production. He called attention also to the special AIEE committee on co-operation with war agencies and to the many ways in which it had assisted (*EE, March '43, p. 119*). Subsections now are being formed within the territories of some AIEE Sections, especially those covering a large territory, to assist in maintaining attendance at Section meetings under wartime transportation restrictions. He mentioned also the special wartime services established for the benefit of the membership, and, as two examples, cited the quarterly news bulletin soon to be issued to AIEE members in the armed services and merchant marine, and the provisions which have been made to enable such men to maintain an inactive membership status, without payment of dues.

What the MVEA is doing to aid the war effort was outlined by Arthur L. Mullergren (M '19) president of that association. He first described briefly the organizational setup and membership of the association and the types of meetings held. The place of the utilities in the war effort has been the theme of MVEA meetings since Pearl Harbor, he declared. The intensified wartime activities in the midwest area have imposed particularly difficult problems on the power companies. Mr. Mullergren briefly described methods and measures which have been taken to cope with those problems. He emphasized the fact that all war-industry demands for electric power had been met, with minimum interference with nonwar activities.

In the fourth address of the session, AIEE President Harold S. Osborne spoke on "The Engineer and the War," discussing both the individual and professional responsibilities of the engineer in the war ef-

E. F. Mahood, AIEE vice-president of the South West District and chairman of the District meeting and Sections committees, stopped to converse with C. W. Nystrom, general outside plant engineer for Southwestern Bell Telephone Company, following one of the sessions of the AIEE South West District technical meeting. Mr. Nystrom spoke on "Conservation of Critical Materials in the Telephone Industry" at the meeting

fort. Full text of his address is presented elsewhere in this issue (*page 233*).

"Electrical Indicating Instruments in Wartime" was the subject of the concluding address, delivered by Harold L. Olesen (M '27) of the Weston Electrical Instrument Corporation, Newark, N. J. He briefly sketched the history of electrical instruments which he said originated in 1819. No new principles have been developed since 1883, he declared, but there have been great changes in the application of those principles and the resultant instruments. He discussed the instruments required in the present war for aircraft, ship, and ordnance use, and characterized the requirements of instruments for ultra-high-frequency use in this war as totally unheard of previously. He indicated the magnitude of the present war requirements by stating that an average of one electrical instrument is required for every soldier. This means an output of from 12 to 15 times normal on the part of the instrument manufacturers. He depicted the present status of electrical instrumentation by striking contrasts and comparisons between the present war and World War I.

JOINT SESSION ON CRITICAL MATERIALS

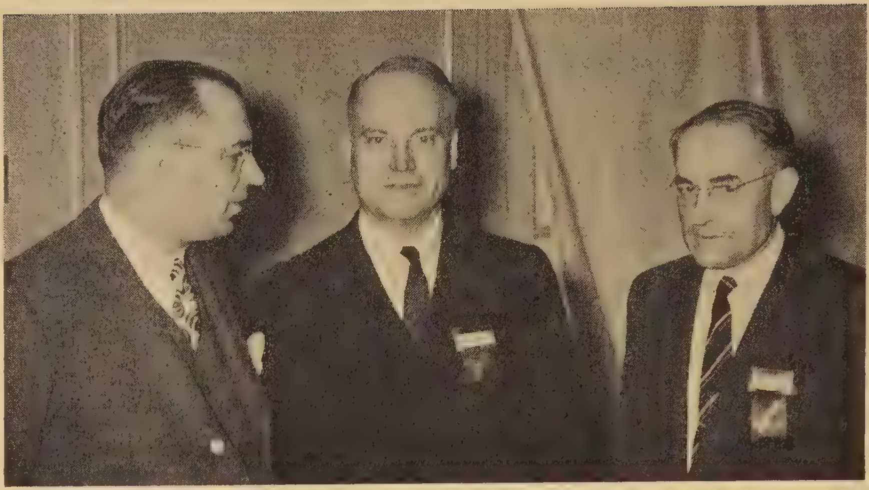
Conservation of critical materials was the theme of the second session held jointly with MVEA during the AIEE South West District meeting. Five addresses on various phases of the subject were presented. AIEE Vice-President E. T. Mahood presided.

Analysis of Registration at Kansas City

Classification	Kansas City Section	District 7*	Other Districts	Totals
Members	98	65	54	217
Enrolled Students	55	24		79
Men guests	15	5	22	42
Totals	168	94	76	338

* Outside Kansas City.

Never before have we known a shortage of raw materials required for industrial manufacture in the United States, said H. M. MacDougal (A '40) of the War Production Board, Washington, D. C., discussing the functions and program of WPB. In citing some of the principal reasons for the present shortages, he drew comparisons and contrasts between World Wars I and II. He said that in World War I, only 94 pounds of metal were required per man, whereas in this war 4,900 pounds per man are required. Other important factors are the greater size of our armed forces now and the requirements of the lend-lease program. Of the four M's representing necessities in the present war-production program—men, machines, materials, movement—the most critical is materials. Substitutions and simplification of design are among the principal conservation measures advocated by WPB. Scrap no longer is considered a nuisance but a treasure; ordinary sources are becoming exhausted, however, and from now on scrap must be "dug" out of obsolete and unused equipment. Mr. MacDougal called attention to the salvage manual prepared some time ago for the power companies and said that a similar salvage manual is being prepared for all industry. In closing, he commended the power industry for its performance in the war effort.



At the AIEE South West District technical meeting: A. C. Monteith, chairman of the committee on power generation, who spoke on "Emergency Measures to Increase Output of Generating Equipment and Systems"; C. M. Laffoon, AIEE director and chairman of a subcommittee of the committee on power generation; and F. J. Meyer, AIEE director and vice-president in charge of operations for Oklahoma Gas and Electric Company, who spoke on "AIEE in the Southwest"

AIEE President Osborne in his address drew statistical comparisons between the amounts of critical metals available to the Axis countries and to the United Nations. The situation as indicated by these figures makes it imperative that we make the greatest possible use of what we have. He recommended the following six expedients to conserve critical metals: substitutions, redesign of products, restriction of plant addition to immediate needs only, standardization, salvage, and reduced inventories. (Full text of Doctor Osborne's address is scheduled for publication in a later issue.)

An address describing the AIEE wartime guides for the selection and operation of electrical equipment, prepared by J. R. North, chairman, AIEE standards committee (who was unable to be present), was read by A. C. Monteith, vice-chairman of that committee. The purpose of these guides is to assist users of electrical equipment to obtain the greatest possible utilization of such equipment under present emergency conditions. (A fuller description of the guides, their history, purpose, and status, is outlined in a separate item, *page 258*.)

Measures being taken by the telephone industry to conserve critical materials were outlined by C. W. Nystrom (M '33) of the Southwestern Bell Telephone Company, St. Louis, Mo. Substitutions, changes in design, and recovery of used materials, he cited as the three principal methods employed. He mentioned many specific instances of substitutions of less critical metals for more critical metals and quoted statistics as to the resultant savings. All used materials and equipment are salvaged, he said, and, whenever possible, reused. He also called attention to the increasing use of carrier communication. Mr. Nystrom said that in spite of wartime operating difficulties, in 1942 three quarters of a billion more telephone calls were handled throughout the system than in any previous year and that there had been a 45 per cent increase in tool traffic.

The last item on the session agenda was on emergency measures to increase output of generating equipment. This presentation constituted a preliminary report on a wartime guide for the operation of electric generating equipment which is being prepared by the AIEE committee on power generation and two of its subcommittees. A. C. Monteith, chairman of that committee, introduced the subject. Measures to increase output of generating equipment were reported by C. M. Laffoon, chairman of the subcommittee on that subject; and emergency measures to increase the output of generating systems, by R. L. Frisby, member of the committee on power generation. (An article summarizing the recommendations made in the final form of this guide is scheduled for publication in an early issue.)

STUDENT ACTIVITIES

In addition to the regularly scheduled events of the meeting, features of especial interest to students were held. These included: a student technical session; two breakfast gatherings, one for Branch officers, and the other for Branch counselors; and student consultations wherein student members could confer with representatives of various industries. H. F. Huffman (M '33) of the Southern Methodist University, Dallas, Tex., was elected District chairman of student activities for the ensuing year, to succeed W. F. Gray (A '38) current chairman.

Student Technical Session. Edward Ancona of the University of New Mexico Branch presided at the student technical session. Four papers were presented by students from four Branches:

1. The Operation of Single-Phase Watt-Hour Meters, **Franklin Fansher,** University of Missouri, Columbia.

2. Revisions of the Electrical Code Due to the War Effort, **L. V. Randle,** Southern Methodist University, Dallas, Tex.

3. Routine Testing of Residential Watt-Hour Meters, **J. G. McDonald,** Kansas State College, Manhattan.

4. A Comparative Study of the Rationalized and Unrationalized MKS Unit Systems, **G. L. Johnson, V. J. McKale,** University of Kansas, Lawrence.

Student Consultations. Arrangements were made whereby students could confer with representatives of a variety of industries on conditions, prospects, and future outlook of those industries, Thursday morning, April 29. The following consultants were on hand, each in a separate room:

Aviation—Luther Hoffman, Transcontinental and Western Airways.

Communication—C. H. Weiser, Southwestern Bell Telephone Company.

Consulting Engineering—R. L. Baldwin (F '37) Burns and McDonnell Engineering Company, Kansas City.

Electrical Manufacturing—George Fiske (A '11) General Electric Company, Kansas City.

Power and Light Operating—G. O. Brown (A '15) Kansas City Power and Light Company.

Transportation—J. A. Parkinson, Atchison, Topeka and Santa Fe Railroad Company

LUNCHEON AND DINNER

Informal talks by national AIEE officers featured the luncheon held Thursday, April 29, during the Kansas City meeting. Students were admitted to the luncheon without charge, each student being assigned a senior member as host. C. G. Roush, chairman of the Kansas City Section presided. At the AIEE dinner Thursday evening, Tom Collins, formerly of Kansas City (now of Omaha, Neb.) and well-known midwest after-dinner speaker, talked on the subject "Ready or Not?" He presented refreshing viewpoints on the current situation. Vice-President E. T. Mahood acted as toastmaster at the dinner.

"The Engineer's Start in Civic Affairs" was the title of a talk prepared by P. L. Alger, chairman, AIEE technical program committee; in Mr. Alger's absence, his talk was read by S. M. DeCamp (M '24) of the General Electric Company, Kansas City. In this talk Mr. Alger suggested how the typical engineering graduate may proceed in seeking both the professional standing and the civic usefulness that accord with the high ideals of the engineering profession. After establishing a professional standing, the young engineer may apply himself to civic problems. The normal procedure for developing his civic standing is exactly similar to that followed in his professional development. However, any undue time or effort spent on pursuing a civic hobby at the expense of his professional work, Mr. Alger warned, will react very strongly against acceptance of his ideas on civic matters.

In the second talk, National Secretary H. H. Henline reviewed AIEE progress in recent years. He traced the fluctuations in membership since the early 30's, through the depression years and into the recovery period. AIEE membership now stands at its highest point in history—more than 20,000 members. Committee structure, Mr. Henline said, has been kept flexible in accordance with current requirements. He mentioned recent changes, consolidations, and new committees appointed. He reported that local, District, and national meetings are now geared very strongly to wartime requirements, in accordance with policies adopted last August. In an effort to assist as much as possible in the war effort, the Institute's board of directors decided to continue a full schedule of national and District technical meetings in spite of transportation difficulties. In conclusion, he said that the AIEE now is in a strong position and that there is every reason to expect it to continue so.

"AIEE in the Southwest" was the subject of the concluding talk at the luncheon, by F. J. Meyer, AIEE director, of Oklahoma City, Okla. Mr. Meyer discussed some of the current problems faced by engineers and engineering societies and their relationships to civic affairs.

SECTIONS CLINIC

Various phases of Section operation and management were discussed at the Sections clinic, and three recommendations to the District executive committee were passed. All Sections within the South West District were represented among the 35 members who attended. Also present were President Harold O. Osborne, National Secretary H. H. Henline, and Sections Committee Chairman E. T. Mahood. C. W. Mier, nominee for AIEE directorship, presided, and A. T. Campbell acted as secretary of the clinic.

The various types of meetings held by the different Sections represented were discussed in some detail. Some indicated a preference for relatively short meetings; others reported best results with dinner meetings. Some favored more joint activities with other local engineering groups, especially in matters of mutual interest such as civic activities. One Section reported that each member of its meetings and papers committee is made responsible for one meeting during the season; this has resulted in some excellent meetings and in the development of candidates for Section offices.

Mr. Mahood expressed the opinion that the general practice of sending incoming Section chairmen to the national summer meeting effectively promotes the welfare of the Institute, but that full advantage is not taken of the experience gained by outgoing Section officers. The ensuing discussion led to adoption of the recommendation that:

> "It be made a part of the duties of the Section chairmen, at the close of their term of office to make a report in writing to the incoming officers, with copies to the District vice-president and the national secretary, covering the Section activities during the year and embodying recommendations for such changes, additions, or omissions in Section operation and management as their experience in office indicates would be beneficial to the Institute."

H. G. Mathewson, secretary of the North Texas Section, pointed out that many Section officers take office June 1, whereas national officers take office August 1. Material pertaining to Sections operations is sent to the new chairmen on the latter date. Mr. Mathewson suggested that such material be sent to Section officers as soon as they take office and suggested also that more complete instructions might help. The following recommendation grew out of the ensuing discussion:

> "A pamphlet, to serve as a manual on Section operation and management, should be prepared and distributed to all newly elected Section officers by the national secretary upon notification of their election. This manual should cover all pertinent features of Section operation and management, duties of Section officers, and relevant parts of the constitution and bylaws."

The need of a clearinghouse for Section-meeting speakers also was discussed and various plans proposed. The discussion of this subject resulted in the recommendation that:

> "It be made a part of the District secretary's duties to serve as a clearinghouse on speakers and programs to facilitate the exchange of information within the District as to the availability of speakers and programs or Section meetings."

President Harold S. Osborne spoke briefly regarding his travels among the Sections, declaring that "he was much impressed with the fact that the life of the Institute is in the Sections and that the future life of the Institute depends on them."

DISTRICT EXECUTIVE COMMITTEE MEETING

Award of District prizes for Student Branch papers, formation of subsections, recommendations passed by the Sections clinic, and the financial problems of Sections that are hosts to District or national meetings, were among the important items discussed at the meeting of the District executive committee. Vice-President E. T. Mahood presided, and District Secretary R. G. Kloeffler acted as secretary of the meeting.

Secretary Kloeffler presented a report covering certain aspects of the award of District prizes for student papers. His report and the ensuing discussion culminated in passage of the following motion:

1. The time of closure for submission of Student Branch papers for consideration for the District award shall be June 30.

2. It shall be the policy of the District to arrange for the judging of these papers as early as convenient after June 30 and to present the award early in the following fall.

3. In any year the granting of the award for the best Student Branch paper may be withheld if less than three papers be submitted or if the quality of the papers submitted be below normal. The decision concerning the quality of the papers is to rest with the judges.

Representatives of all Sections in the District reported that careful consideration had been given to the possibility of forming subsections. All but two Sections reported that subsections do not seem to be desirable. S. C. Commander, secretary of the Houston (Tex.) Section, reported that speakers at meetings in Houston sometimes repeat their talks at additional meetings in Beaumont. F. A. Decker, chairman of the New

Mexico–West Texas Section, reported that Albuquerque is 300 miles from Section headquarters and that it needs a subsection.

Action was taken providing for the establishment of a District fund to assist host Sections in financing District and national meetings. To start the fund, the Kansas City Section agreed to turn over any funds remaining from the special collection made to finance the current District meeting. The plan calls for contributions from the Sections at the rate of 25 cents per member annually. The District secretary will act as custodian, and payments from the fund are to be made only with the approval of the chairman of the Section holding a District or national meeting, the District secretary, and the District vice-president. Final approval of the plan rests with the executive committees of the Sections.

Three recommendations made at the clinic on Section operations and management (reported separately) were adopted by the District executive committee. The recommendation pertaining to the preparation of a manual on Section operation and management is to be referred to the committee on Sections by Vice-President Mahood, who is chairman of that committee.

A proposal to hold the next District meeting at San Antonio, Tex., was discussed informally, and tentatively approved.

COMMITTEES

E. T. Mahood, vice-president representing the South West District, was chairman of the meeting committee. Other members were A. T. Campbell, P. C. Ellis, W. F. Gray (District chairman of student activities), R. G. Kloeffler (secretary, South West District), I. T. Monseth (chairman, St. Louis Section), Ralph Randall (chairman, Oklahoma City Section), C. G. Roush (chairman, Kansas City Section), P. H. Underwood (chairman, Houston Section), and C. V. Waddington.

Subcommittee chairmen were as follows:

Meetings and Papers—V. P. Hessler.
Student Activities—R. L. Baldwin.
Inspection Trips—A. E. Bettis.
Publicity and Attendance—A. T. Campbell.
Finance—George Fiske.
Hotels and Registration—C. G. Roush.
Luncheons and Dinners—P. C. Ellis.

Four Technical Sessions Held at Kansas City Meeting

Sessions on electrical machinery, communication, transportation, and selected topics comprised the technical program of the recent South West District meeting in Kansas City, Mo. Conservation of materials, emergency loading and utilization of electrical equipment, teaching of wartime subjects, and special practices developed to meet wartime needs were among the most important subjects discussed.

ELECTRICAL MACHINERY

Design and construction features of the Oilostatic transmission system and its applications were described by Charles E. Bennett (F '25) of the Okonite-Callender Cable Company, Inc., Paterson, N. J. This system consists of one or more paper-insulated cables installed in a steel pipe which is filled with insulating oil under high pressure. Mr. Bennett presented operating data on the first system, which was installed in 1932 on a 66-kv line, and which, he said, had operated successfully. Satisfactory operation was reported on other installations ranging in voltage up to 132 kv. Ten years' experience in handling and installing nonleaded paper cables in steel pipes has demonstrated that this is not injurious to the cable.

There is no safe overload rule for utilizing the latent emergency capacities of transformers, said J. K. Hodnette (F '42) of the Westinghouse Electric and Manufacturing Company, Sharon, Pa., who spoke on conservation of critical materials in transformers. However, he described a simple thermoelectric relay, the thermal characteristics of which match those of the ordinary transformer. He also described the use of Hipersil, an improved silicon steel for transformer cores. The use of this material results in a transformer of smaller physical dimensions which means the saving of materials in all parts of the unit; the average saving is said to be 20 per cent as compared with transformers using ordinary silicon steel. With regard to transformers already in service, Mr. Hodnette pointed out that air-blast cooling and provisions for circulating the oil within the transformer tank would each increase the rating of a power transformer by one third. By utilizing both these expedients, 45 per cent less materials are required for a given transformer output.

A paper "Effect of Control on the Characteristics of D-C Series and Shunt Motors," presented by A. A. Merrill of the General Electric Company, Schenectady, N. Y., showed how the characteristics of these motors may be "twisted" into almost every desirable form by simple manipulation of external resistors and connections. Mr. Merrill presented many charts showing the speed-torque relationships for a variety of types of control connections and explained their application to specific problems.

"Normal and Emergency Loading of Power Transformers," by H. B. Keath (F '39) and W. L. J. F. Godin of the Wagner Electric Corporation, St. Louis, Mo., was presented by Mr. Godin. In this paper the authors endeavored to unify and to bring up to present-day practice the different viewpoints on this subject. They contended that all groups and subdivisions of transformers can be brought back to the fundamental case of the 55-degree-centigrade-rise self-cooled transformer and that the various groups differ only in their external features—that is, in the method of cooling and conditions of loading. This simplifies the procedure of determining emergency overloads. Standardized rules for normal loading or loading for normal life expectancy simply become functions of the average daily ambient temperatures, the authors concluded.

"Graphic Solutions in Unbalanced Transformer Loading," by R. H. Jones, Jr. (A '41) of the Moloney Electric Company, St. Louis, Mo., was presented by W. C. Wooley (Mr. Jones is now on active duty as a lieutenant in the United States Naval Reserve). This paper outlined the development and application of several graphical constructions in the solution of problems in unbalanced transformer loading.

O. T. Farry (M '40) of the Wagner Electric Corporation, St. Louis, Mo., presided at this session.

COMMUNICATION

The session on communication began with a paper on wartime courses in ultrahigh-frequency techniques, by R. E. Galbraith (A '38) of the University of Texas, Austin. He said that it had been found necessary to give not just training in ultrahigh-frequency work but complete training in radio, because the majority of students had had no previous radio training. The course, which requires 16 weeks for completion, is divided into four parts:

1. Smoothing circuits.
2. Transients.
3. Antennas and wave guides.
4. Negative-grid tubes.

He said that in the beginning no textbooks nor laboratory apparatus were available; consequently, special "class notes" had to be prepared and makeshift apparatus pieced together out of parts collected from a variety of sources. In the discussion of this paper it was pointed out that the expanding applications of ultrahigh frequencies will require a change in the method of teaching the fundamentals of electrical transmission.

The paper, "Train Communications," by L. O. Grondahl (M '42) and P. N. Bossart of the Union Switch and Signal Company, Swissvale, Pa., was presented by G. A. Richardson (A '42) of the University of Kansas, Lawrence. The paper describes a carrier system for communicating between railway vehicles and between such vehicles and fixed points. (This paper is scheduled for publication in the July *Transactions* section.)

Communication facilities provided for civilian-defense purposes were described by O. S. McDaniel of the Southwestern Bell Telephone Company, Dallas, Tex. He described:

1. The aircraft warning system whereby information on airplanes is communicated to Army authorities.
2. The air-raid warning system whereby the Army communicates its instructions to the air-raid wardens.
3. The provisions enabling the wardens to transmit information to the public and to summon any required services.

He described the so-called conference system whereby from 10 to 20 telephone calls may be made simultaneously, thus facilitating the sending out of warnings to danger areas. He also mentioned the war emergency radio service established for local communication when other means fail (*EE, May, '43, p. 224*).

The final paper of the session, by G. A. Dyer (M '39) of the Southwestern Bell Telephone Company, Dallas, Tex., described the recently installed Dallas–San Antonio intercity toll cable. Mr. Dyer first traced the fluctuations in telephone traffic on his company's system from the late 20's, through the depression years, and into the present period of intensified activity. Extensive use of carrier of the so-called "*J*" and later the "*K*" types have assisted materially in meeting wartime demands for increased communication facilities. Mr. Dyer then described the twin cables installed between the two cities and told how the cables were buried in the ground and covered, all in one continuous operation, by a special plow train. His oral presentation was supplemented by a motion picture showing the operation of the plow train and some of the many obstacles encountered on the right of way.

H. R. Fritz (F '39) Southwestern Bell Telephone Company, St. Louis, Mo., presided at the communication session.

TRANSPORTATION

Operating experiences with modern rapid-transit equipment was the general subject of four papers presented at the transportation session. I. T. Monseth (M '39), Westinghouse Electric and Manufacturing Company, St. Louis, Mo., presided.

The advantages of high-speed traction motors were discussed by C. A. Atwell (A '41), Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa., in the first paper. (This paper is scheduled for publication in the July *Transactions* section.)

Although we are now engaged primarily in a war of production of materials and transportation of men and materials to the scene of the fighting, of almost equal importance is the transportation of workers between their homes and the plants producing war materials, said G. M. Woods, Westinghouse Electric and Manufacturing Company, in his paper "Maximum Utilization of Transit Equipment." He outlined methods in use by rapid-transit companies in handling the unprecedented increase in traffic brought about by the war. Some of the expedients discussed are: rehabilitation of retired cars; puchase of second-hand cars, previously released from abandoned routes; restoration of streetcar service where tracks still exist; improvement of scheduling; and purchase of new cars where extreme need can be shown. Other at least equally important steps, he said, rest with the communities themselves. Among these are staggering hours, reducing number of stops, and facilitating the movement of mass-transportation vehicles by giving them preference in traffic regulation.

Kansas City's wartime experiences and practices in mass transportation were described in a paper by L. L. Davis (M '36) and G. H. Frieling, of the Kansas City Public Service Company, presented by Mr. Davis. Riding in Kansas City for the six months prior to March 1, 1943, increased 75 per cent over the corresponding period prior to March 1, 1941, he said. To date it has been possible to supply the increased demand. Vehicles that formerly were operated with partial loads absorbed much of this increase. Old discarded equipment has been rehabilitated and placed in service. The changes instituted by the Office of Defense Transportation have added an equivalent 289 vehicles to the fleet and saved an estimated 2,961,215 vehicle-miles per year, Mr. Davis added. He mentioned the splendid co-operative spirit of the public and the additional burden on maintenance facilities.

Membership Passes 20,000 Mark

Institute Members:

The Institute has crossed the 20,000 mark in membership. This accomplishment has, in large measure, been due to the co-operation which we have received from individual members. The names which many of you have suggested have given us a substantial base upon which to build. We want you to know that this effort on your part is appreciated.

(*Signed*) JOHN H. PILKINGTON
Chairman, National Membership Committee

In the final paper, F. Norman Hill of the St. Joseph (Mo.) Railway, Light, Heat, and Power Company, summarized 11 years' operating experience with trolley coaches. He presented some interesting comparisons between trolley and motor coaches. The trolley coach is approximately 15 per cent faster because of its better accelerating characteristics, and its negotiating grades more easily. The trolley coach has a higher first cost, but lower operating and maintenance costs and longer life. Public reaction favors the trolley coach, as reflected by traffic increases.

SELECTED TOPICS

Five papers on many different subjects were presented at the selected-topics session at which G. T. Shoemaker (F '39), Kansas City Power and Light Company, presided.

G. C. Quinn, Allis-Chalmers Manufacturing Company, Milwaukee, Wis., reviewed wartime trends in arc welding. Although arc welding has been used for many years in maintenance and repair work, only recently has it been applied to production. Although it started slowly, the increase in application during the current war period has been tremendous. To illustrate, Mr. Quinn said that $18,000,000 were spent on arc welding in 1938, but in 1942 the expenditure for the year had risen to $128,000,000. He reviewed the progress in welding techniques, electrodes, and machines. Standard techniques are needed, he said, and this problem is only halfway solved. Electrodes, which were poor at the beginning, have been improved vastly, and small percentages of alloying elements are now being added to the rod coatings. He predicted further improvement and standardization of welding rods. In regard to equipment, he said that d-c motor-generator sets had been developed highly and had not been changed much in recent years. Electronic welders are proving best for thin materials. Transformer welders were initially unsatisfactory, and many operated at too high voltages; with the introduction of coated electrodes, these voltages were reduced. A-c welders giving continuously variable output current later were developed; today a-c welders comprise a large part of new equipment installations.

One of the few safety engineering courses in the United States was described by A. Naeter (M '30), Oklahoma Agricultural and Mechanical College, Stillwater. The course consists of three parts:

1. Physiology and industrial hygiene.
2. Safety engineering and accident prevention.
3. First aid.

Lectures are given by men engaged in actual safety work and also by faculty members. First-aid instruction is given by a representative of either the Bureau of Mines or of the Red Cross. The instruction is supplemented by practical hints for safe working procedures.

The third paper of this session, by G. F. Leland and L. T. Rader (A '34) of the General Electric Company, Schenectady, N. Y., presented a mathematical analysis of dynamic braking of a d-c shunt motor and its load. (This paper is scheduled for publication in the July *Transactions* section.)

Amplidyne circuits were described and performance characteristics analyzed in the fourth paper, by F. E. Crever (A '27) of the General Electric Company, Schenectady, N. Y.

Inductive heating in ferrous-metal equipment members adjacent or in close proximity to conductors in switchgear was the subject of the final paper, presented by Stanley C. Killian (A '42) Delta-Star Electric Company, Chicago, Ill. With the present emphasis on conservation of copper and other nonferrous metals, it is important that the less critical metals be used wherever possible for fittings and enclosures. Mr. Killian summed up the data presented in his paper in the following approximate manner:

> "Up to 600 amperes, little precaution needs to be taken against magnetic heating, and steel parts can be used almost indiscriminately. From 600 to 3,000 amperes, the problem must be given careful consideration, and nonferrous parts must be used in conjunction with ferrous parts. Above 3,000 amperes, all parts should be nonferrous."

Section and Branch Activities—Annual Report for 1942-43

The following constitutes the annual report on Institute Section and Branch activities for the fiscal year which ended April 30, 1943. Similar information for three preceding fiscal years appeared in *Electrical Engineering* in June 1942, pages 322–3; June 1941, pages 285–7; June 1940, pages 250–3.

Present members of the Sections committee and the committee on Student Branches, which supervise the divisions of Institute activities covered by this report, are:

Sections—E. T. Mahood, chairman; J. M. Thomson, vice-chairman; E. W. Schilling, secretary; O. C. Brill, M. S. Coover, C. A. Faust, H. L. Hazen, S. J. Lisberger, W. B. Morton, R. M. Pfalzgraff, and ex officio the chairmen of all Sections of the Institute.

Branches—R. G. Warner, chairman; E. W. O'Brien, vice-chairman; H. W. Bibber, L. A. Bingham, P. L. Hoover, Everett S. Lee, W. J. Miller, A. Naeter, B. L. Robertson, G. H. Sechrist, V. G. Smith, E. M. Strong, G. W. Swenson, and ex officio all Student Branch counselors.

Table I. Section Membership and Meetings During Year Ending April 30, 1943

Section	AIEE Members August 1941	AIEE Members August 1942	Number of Meetings	Average Attendance
Akron	87	94	8	67
Alabama	45	44	2	17
Arizona	28	37	8	29
Boston	439	516	8	220
Central Indiana	144	133	9	213
Chicago	810	836	6	239
Power group			4	64
Industrial group			3	69
Communication group			3	97
Cincinnati	212	214	6	134
Dayton Subsection			1	
Cleveland	363	376	4	
Technical group			1	74
Columbus	86	95	5	328
Zanesville Subsection				
Connecticut	299	333	5	81
Denver	177	180	8	62
East Tennessee	137	145	14	232
Erie	56	65	6	60
Florida	100	111	1	12
Fort Wayne	80	93	9	65
Georgia	122	119	3	138
Houston	141	133	9	66
Iowa	81	84	11	98
Ithaca	55	53	4	53
Kansas City	128	140	9	130
Lehigh Valley	194	188	6	97
Los Angeles	507	540	11	192
Aviation division			1	
Louisville	86	73	9	87
Lynn	167	186	8	375
Madison	72	73	5	62
Rock River Valley Subsection			8	77
Mansfield	77	71	8	43
Maryland	286	322	8	104
Memphis	66	56	9	29
Mexico	57	53	7	38
Michigan	398	418	9	125
Round table meetings			2	
Milwaukee	286	313	10	155
Minnesota	103	98	10	53
Power group meetings			3	52
Montana	40	45	5	27
Montreal			1	38
Muscle Shoals	30	33	7	20
Nebraska	45	59	5	53
New Mexico–West Texas	43	43	8	17
New Orleans	132	133	9	39
New York	3,492	3,447	4	338
Power group			5	211
Communication group			2	95
Transportation group			2	135
Illumination group			3	178
Basic science group			4	93
Niagara Frontier	207	198	8	60
Niagara Falls Subsection			1	
North Carolina	101	107	1	68
North Texas	181	170	8	77
Oklahoma City	117	104	10	88
Philadelphia	750	809	11	207
Instruments and measurements discussion group			2	140
Communication discussion group			3	85
Industrial practice discussion group			2	41
Power discussion group			4	81
Foremen's discussion group			1	42
Pittsburgh	630	662	11	148
Pittsfield	197	194	6	771
Popular lectures			6	
Portland	196	196	10	71
Transmission and distribution committee			2	50
Providence	95	100	8	43
Rochester	110	112	11	90
St. Louis	304	360	9	102
San Diego	46	54	8	41
San Francisco	543	549	9	138
Saskatchewan	10	8	1	
Schenectady	484	559	7	193
Seattle	202	230	9	82
Sharon	120	153	9	67
South Bend	52	58	10	58
South Carolina	50	53	2	54
South Texas	48	53	6	31
Spokane	84	82	9	46
Springfield	51	52	9	81
Syracuse	103	119	7	79
Toledo	80	89	9	60
Toronto	363	396	14	153
Tulsa	90	87	8	46
Urbana	68	78	6	112
Illinois Valley Subsection, Peoria				
Utah	72	64	9	45
Vancouver	91	95	9	67
Virginia	137	150		
Washington	491	600	9	219
West Virginia	54	39	1	200
Wichita	64	57	10	42
Worcester	65	63	7	97
Total Sections 73	15,927	16,652	598	
Total attendance, 66,111				

Table II. Branch Meetings Held During Year Ending April 30, 1943

Branch	Number	Average Attendance
Akron, University of	6	10
Alabama, Polytechnic Institute	5	50
Alabama, University of	7	19
Alberta, University of	5	23
Arizona, University of	29	14
Arkansas, University of	9	14
British Columbia, University of	17	21
Brooklyn, Polytechnic Institute of		
Day division	6	23
Evening division	6	12
Brown University	3	17
Bucknell University	4	25
California, Institute of Technology	5	38
California, University of	4	41
Carnegie Institute of Technology	25	69
Case School of Applied Science	5	29
Catholic University of America	2	21
Cincinnati, University of	10	27
Clarkson College of Technology	4	20
Clemson Agricultural College	5	27
Colorado State College	4	17
Colorado, University of	8	35
Columbia University	4	30
Connecticut, University of	3	28
Cooper Union		
Day division	6	29
Evening division		
Cornell University	3	56
Delaware, University of	4	23
Denver, University of	7	25
Detroit, University of	6	31
Drexel Institute of Technology	12	24
Duke University	7	21
Florida, University of	9	20
George Washington University	11	23
Georgia School of Technology		
Harvard University		
Idaho, University of	14	55
Illinois Institute of Technology	2	46
Illinois, University of	1	440
Iowa State College	8	52
Iowa, University of	33	23
Johns Hopkins University	2	31
Kansas State College	12	72
Kansas, University of	8	34
Kentucky, University of	20	98
Lafayette College	4	17
Lehigh University	1	60
Louisiana State University	7	22
Louisville, University of	7	19
Maine, University of	2	16
Manhattan College	4	25
Marquette University		
Maryland, University of	7	19
Massachusetts Institute of Technology		
Michigan College of Mining and Technology	12	34
Michigan State College	8	27
Michigan, University of	9	29
Milwaukee School of Engineering	3	44
Minnesota, University of	5	26
Mississippi State College	7	23
Missouri School of Mines and Metallurgy	3	28
Missouri, University of	5	46
Montana State College	10	27
Nebraska, University of	13	40
Nevada, University of	8	22
Newark College of Engineering	5	47
New Hampshire, University of	10	25
New Mexico State College	5	30
New Mexico, University of	11	28
New York, College of the City of		
Day division	16	71
Evening division		

Table II (continued). Branch Meetings Held During Year Ending April 30, 1943

Branch	Number	Average Attendance
New York University		
Day division	4	38
Evening division	3	27
North Carolina State College	11	33
North Dakota Agricultural College		
North Dakota, University of	4	15
Northeastern University	7	48
Northwestern University	1	55
Norwich University	4	18
Notre Dame, University of	8	19
Ohio Northern University	14	21
Ohio State University	12	33
Ohio University	2	23
Oklahoma Agricultural and Mechanical College	8	32
Oklahoma, University of	7	24
Oregon State College	5	30
Pennsylvania State College	6	125
Pennsylvania, University of	4	21
Pittsburgh, University of	43	72
Puerto Rico, University of	1	30
Pratt Institute	16	33
Princeton University	3	
Purdue University	6	76
Rensselaer Polytechnic Institute	8	46
Rhode Island State College	2	14
Rice Institute	9	26
Rose Polytechnic Institute	10	31
Rutgers University	4	16
Santa Clara, University of	11	21
South Carolina, University of	13	20
South Dakota State College	9	21
South Dakota State School of Mines	8	27
Southern California, University of	7	41
Southern Methodist University	9	28
Stanford University	6	27
Stevens Institute of Technology		
Swarthmore College		
Syracuse University		
Tennessee, University of	13	22
Texas Agricultural and Mechanical College	3	39
Texas Technological College	9	29
Texas, University of	9	41
Tufts College	8	32
Tulane University	8	23
Union College	8	30
Utah, University of	6	26
Vanderbilt University		
Vermont, University of	20	24
Villanova College	8	9
Virginia Military Institute	2	44
Virginia Polytechnic Institute	28	42
Virginia, University of	5	13
Washington, State College of	13	32
Washington, University of	8	47
Washington University	4	25
West Virginia University	5	29
Wisconsin, University of	6	30
Worcester Polytechnic Institute	3	68
Wyoming, University of	4	18
Yale University	4	58
Total—125 Branches		

Table III. Section Meetings Held During Last Three Fiscal Years

	Fiscal Year Ending April 30		
	1941	1942	1943
Number of Sections	72	72	73
Number of meetings held	703	647	598
Average number of meetings	9.8	9.0	8.2
Total attendance	92,554	78,254	66,111
Average attendance per meeting	132	121	112

Table IV. Branch Meetings Held During Last Three Fiscal Years

	For Year Ending April 30		
	1941	1942	1943
Number of Branches	123	124	125
Number of meetings held	1,163	946	942
Average number of meetings	9.5	7.6	7.5
Total attendance	52,285	37,785	38,227
Average attendance per meeting	45.0	39.9	40.6

Table V. Conferences on Student Activities

District	Location	Date
5	Northwestern University, Evanston, Ill.	5/2/42
8 and 9, University of British Columbia	Vancouver, B. C., Canada (Pacific Coast convention)	9/9–11/42
2	University of Pittsburgh, Pittsburgh, Pa.	10/16/42
7	Kansas City, Mo. (South West District meeting)	4/28/43

Table VI. Section or Joint Section and Branch Meetings With Active Student Participation

Sections	Branches	Date	Student Talks	Attendance
Maryland	Johns Hopkins University	5/4/42	3	90
Lehigh Valley	Lafayette College Lehigh University	5/8/42	1	82
Oklahoma City	University of Oklahoma	5/13/42	2	94
Portland	Oregon State College	5/16/42	3	67
South Bend	University of Notre Dame	12/8/42	4	32
South Texas	University of Texas	12/14/42	5	45
Akron	University of Akron	1/12/43	5	46
Vancouver	Univ. of British Columbia	2/16/43	4	65
Houston	A. & M. College of Texas Rice Institute	3/22/43	5	60
Cincinnati	University of Cincinnati	4/1/43	4	153
Oklahoma City	University of Oklahoma	4/7/43	2	65
Iowa	University of Iowa	4/15/43	8	31
New Orleans	Tulane University	4/16/43	2	54
Totals —13 Sections, 14 Branches, 13 meetings			48	884

Nebraska Student Branch Passes 50-Year Mark

The University of Nebraska AIEE Student Branch founded in 1893 as the Electrical Engineers Society celebrated its 50th anniversary May 5, with a dinner at the Lincoln (Nebr.) YWCA. Reminiscences of the original society's activities by former members keynoted the meeting.

In 1907 the older society received Branch standing in AIEE. The Nebraskans recalled that three former AIEE presidents have come from their ranks: Bion J. Arnold, president 1903–04; Dugald C. Jackson, president 1910–11; and Harris J. Ryan, president 1923–24. J. G. White, founder of the New York engineering firm and AIEE vice-president from 1907 to 1909, was also an early member.

The celebrants were reminded of the origin of a Branch custom which has persisted in the college's "Engineers' Night" ceremonies. Under the spell of the 1893 World's Fair at Chicago, Ill., R. B. Owens established in 1895 an annual show of the latest electrical developments. To the electrically naive public of the 1890's it held many of the elements of a sideshow. Demonstrations of the electric welding machine, the water-pail forge, the glowing Crooke's tubes, the silver plating of pennies, the electrolytic decomposition of water, the traveling "electric-light man" with bulbs fastened to his clothing, the arc furnaces, and the operation of dynamos, arc lamps, and other equipment were on the first program. K. C. Randall (M '12) was listed as "director of exhibits." One of the two Edison bipolar generators which Professor Owens brought back from the Chicago fair is now in the Ford museum at Dearborn, Mich.

Evidence that the present members are keeping the high standards set by their predecessors lies in the fact that three of the last five annual prizes for the best student paper in the North Central District have been awarded the Nebraska Branch.

A paper, "Effect on Telephone Circuits of Induction Motor Harmonics," presented by I. M. Ellestad (A '24), and a short history of the Branch culled from the early minutes of the society, presented by a student completed the anniversary program. Congratulatory telegrams were received from H. S. Osborne, AIEE president, and A. L. Jones, AIEE vice-president for the North Central District. L. A. Bingham, nominee for the North Central District vice-presidency, is Branch counselor.

Schenectady Section Organizes New York Production Clinic

Following the expressed proposal of the War Production Board that managers, plant engineers, production men, shop superintendents, and foremen give others the benefit of their production experience to foster more intense development of our resources for total war, the Schenectady Section initiated a Northeastern New York War Production Conference jointly sponsored by the engineering societies of the region. The one-day conference was held April 15 at the Hotel Van Curler, Schenectady.

The main work of the meeting was conducted in two groups of four panel sessions held at 4 p.m. and 8 p.m., respectively. Each session was sponsored by the engineering society most interested in the problem under consideration.

The afternoon sessions discussed: operating problems and maintenance, sponsored by the New York State Society of Professional Engineers; employment, training, and women in industry, arranged by the American Society of Mechanical Engineers; metallurgy and materials substitution, under the sponsorship of the American Society for Metals; and, electric power supply, sponsored by AIEE.

Evening panels were: electric motor and control problems, led by AIEE; inspection and quality control, arranged by ASME; welding, organized by the American Welding Society; and, corrosion and metal protection, under the leadership of the American Chemical Society.

To solve any individual problems confronting those attending the conference a special professional board for consultation was set up. The board of specialists, composed of members of the Schenectady works laboratory, General Electric Company, was prepared to give advice in the fields of metals and die casting, forgings, paints and varnishes, fusion welding, metallography and X ray, chemistry, die and tool steels, metallurgy, resistance welding, refractories, foundry, lubricants, brazing and soldering, and cutting tools.

At the general meeting which opened the conference at 2:00 p.m., addresses by Colonel J. L. Walsh of the Army Ordnance Association, and L. A. Umansky (M '27) of the industrial engineering department of the General Electric Company, were featured. J. C. Page (A '41), conference chairman, presided. An address, "Education for Management" by Harold V. Coes, ASME president, was the main event of the dinner held at 6:30 p.m. T. M. Linville (M '34), chairman AIEE Schenectady Section presided.

Other engineering societies which co-operated in planning the conference were: American Society of Tool Engineers, the Society of Engineers of Eastern New York, the Society of Automotive Engineers, the American Society of Civil Engineers, and the Albany (N. Y.) Society of Engineers.

1943 Lamme Medal Nominations Due December 1

Special attention is directed to the fact that the names of Institute members who are considered eligible for the AIEE Lamme Medal, to be awarded early in 1944, may be submitted by any member in accordance with section 1 of article VI of the bylaws of the Lamme Medal committee, as quoted in the following:

> The committee shall cause to be published in one or more issues of *Electrical Engineering*, or of its successors, each year, preferably including the June issue, a statement regarding the "Lamme Medal" and an invitation for any member to present to the national secretary of the Institute by December 1, the name of a member as a nominee for the medal, accompanied by a statement of his "meritorious achievement" and the names of at least three engineers of standing, who are familiar with the achievement.

Each nomination should give concisely the specific grounds upon which the award is proposed, and also a complete detailed statement of the achievements of the nominee to enable the committee to determine its significance as compared with the achievements of other nominees. If the work of the nominee has been of a somewhat general character in co-operation with others, specific information should be given regarding his individual contributions. Names of endorsers should be given as specified in the foregoing quotation.

STANDARDS . . .

Revision of Marine Rules for War Emergency Now Available

The AIEE committee on marine transportation at the request of the War Production Board has reviewed "Recommended Practice for Electrical Installations on Shipboard (AIEE Standard 45)." As a result of this review there has been developed a modification of and Supplement to Standard 45. These modifications are issued solely for the purpose of conserving critical materials. The use of the modifications will result in a shipboard electrical installation of a lower standard than is contemplated by the regular standard but, in view of the present emergency, is considered satisfactory for the purpose intended. The modification of Standard 45 must be used in conjunction with that pamphlet. Copies of the new publication, known as "45*A*—Modification of and Supplement to Recommended Practice for Electrical Installations on Shipboard," are now available at a cost of 50 cents each with the usual discount of 50 per cent to AIEE members on single copies. Copies may be procured from AIEE headquarters, 33 West 39 Street, New York, N. Y.

ABSTRACTS . . .

TECHNICAL PAPERS previewed in this section will be presented at the AIEE national technical meeting, Cleveland, Ohio, June 21-25, 1943, and are expected to be ready for distribution in advance pamphlet form within the current month. Copies may be obtained by mail from the AIEE order department, 33 West 39th Street, New York, N. Y., at prices indicated with the abstract; or at five cents less per copy if purchased at AIEE headquarters or at the meeting registration desk.

Mail orders will be filled
AS PAMPHLETS BECOME AVAILABLE

Automatic Stations

43-84—Interim Report on Application and Operation of Automatic-Reclosing Equipment on Stub Feeders; *AIEE subcommittee on a-c automatic reclosing equipment. 20 cents.* This report presents the results of a survey which has been made to obtain data on typical present-day a-c automatic-reclosing equipment and practices as applied to stub feeders having a single end source of power. The data received provide a good summary of the reclosing practices in general use and the performance being obtained. They also give an indication of the possibilities of using immediate initial reclosure more extensively for some types of loads. This subject is of timely interest because of the possibilities of conserving critical materials in such cases where the application of automatic-reclosing equipment will defer the necessity of providing additional circuit facilities.

Future AIEE Meetings

National Technical Meeting
Cleveland, Ohio, June 21–25, 1943

National Technical Meeting
Salt Lake City, Utah, September 2–4, 1943

Southern District Meeting
Roanoke, Va., November 16–18, 1943

43-101—Interim Report on Characteristics and Performance of Conductors for Supervisory Control and Telemetering; *AIEE subcommittee on characteristics and performance of conductors for supervisory control and telemetering. 25 cents.* The report summarizes data covering 189 circuits totaling about 1,200 circuit-miles and about 9,000 circuit-mile-years. Whereas the amount of data may not be adequate to represent completely the practices and experiences with respect to conductors used for supervisory control and telemetering, certain generalizations seem permissible. The indications are that

1. About 15 per cent of the circuits are over ten miles (route) in length.
2. Slightly over half are privately owned.
3. Less than 15 per cent have any open-wire construction.
4. Only about ten per cent use a ground-return path.

It is indicated that the pilot conductors account for out-of-service time less than 1/20 of one per cent of the total time. The differences between the performance of privately owned and leased circuits are not large; the performance of both types of conductors is good, and continued improvement is indicated. Pilot conductors in cable, with automatic supervision and with properly designed and co-ordinated terminal protection, seem to be preferred in future installations.

43-86—Experience With Oil Circuit Reclosers on REA Systems; *Lee M. Moore (M '40), Bruce O. Watkins (A '43). 20 cents.* For three years surveys have been made of the operation of small single-pole automatic-reclosing oil circuit breakers (also called "oil circuit reclosers" and "service restorers") on rural electric distribution systems financed by the Rural

Electrification Administration. Results of these surveys indicate that the ratio of lockouts to operations is less than five per cent in most cases, and that there are from four to nine operations per month per breaker during the spring and summer, with each breaker controlling about 20 miles of line. The number of breaker failures and other breaker difficulties have been a small percentage of the total number of breakers installed. These surveys, together with discussions with system managers, have indicated that automatic-reclosing breakers offer many advantages for rural line sectionalizing. Presently available breakers can be much improved in operating characteristics, and new sectionalizing devices are needed. The paper presents suggestions for such improvements.

Basic Sciences

43-106—Formulas for Calculating Short-Circuit Forces Between Conductors of Structural Shape; *Thomas James Higgins* (*A '40*). *25 cents.* The general theory essential to calculation of the short-circuit electromagnetic force exerted between two long parallel conductors with cross sections comprised of arbitrary configurations of parallel-sided rectangular areas is developed through use of basic electromagnetic principles. Application of this theory encompasses that already developed for the special cases of two conductors having coplanar axes and identical parallel-sided rectangular or hollow rectangular cross sections. It affords calculation of the short-circuit electromagnetic forces exerted among the conductors of any d-c single-phase or polyphase bus comprised of conductors of structural shape (rectangular straps, rectangular tubing, channels, angles, tees, *I* beams, and the like), a category not *generally* treated hitherto in the literature, though the shapes just mentioned and others are now in common use. Corroborative of the general theory and illustrative of application of it, specific formulas are obtained for the electromagnetic force exerted between the symmetrically located conductors of a single-phase bus comprised of two straps and two channels back-to-back. The principal literature on calculation of the short-circuit forces exerted on bus supports is mentioned in the paper.

43-107—A Method for Determining the Normal Modes of Foster's Reactance Networks; *Wilbur Reed LePage* (*A '34*). *15 cents.* A Foster reactance network may be a series combination of antiresonant circuits or a parallel combination of resonant circuits. The zeros of the impedance function of the former or of the admittance function of the latter are the normal modes for their respective voltage or current excitation. The function is written in the partial-fraction form, and all but two of the terms are replaced by constants which are determined from a knowledge of an upper (or lower) bound of the required zero. The resultant expression may be solved for zero, as a quadratic, to give a new lower (or upper) bound, respectively. Continued application of this process gives two sequences, converging on the zero from above and below. There is a zero between each pair of poles; and, since these are known from the network structure, they may be used as the bounds of the zero, in the initial application of the process.

43-108—A Method for Correlating Duty-Cycle Tests on Solenoids; *J. E. Ryan. 15 cents.* A method is presented by which a relatively small amount of test data can be correlated and used to predict the steady temperature rise of a particular solenoid when the latter is operating cyclically with any combination of load, stroke, and timing. Although it has been used chiefly for a-c solenoids, the method is applicable to any device which has a cycling period substantially less than its thermal time constant.

Civic Affairs

43-122-ACO—The Engineer's Interest in City Planning; *F. A. Cowan* (*M '29*). *20 cents.* A report of the conference on City and Regional Planning at the Northeastern District meeting, Pittsfield, Mass., April 8, is to be made available at the summer meeting. The report includes a résumé of the principal addresses and discussions by a number of civic officials and experts who are active in planning work. The conference covered such topics as the relation of industrial research to regional development and city planning and the role of planning in a world at war with special attention to the problems of the postwar period. Throughout the discussions emphasis was placed on the contributions the engineer can make to planning programs and on the advantages of organizing the efforts of the engineers along these lines to make the most effective use of their knowledge and training. The necessity for a full appreciation by the engineers of the problems of financing civic improvements and obtaining the co-operation and approval of the public was also stressed. The report closes with a summary of the conclusions of the conference and suggestions regarding the future interest of engineers in city planning as an aid in formulating their long-range engineering programs.

Communication

43-120—Subcenter Switching Systems for Teleprinter Tie Lines; *F. L. Currie. 20 cents.* One of the means used by the telegraph companies for improving the speed of their service is the provision of printing telegraph circuits between their central offices and the offices of many users of the telegraph. In the beginning, circuits of that type were restricted to patrons whose volume of telegraph business and proximity to a central office justified the expense involved in their installation and maintenance. A large part of that expense, when the patrons are located remotely from the central office, is in the line wire costs. This paper describes an automatic-switching means which economically enables a large number of patrons, when grouped in one locality, to be furnished printing telegraph service with a distant central office by causing them to share the use of a comparatively small number of line wires between that locality and the distant central office. The paper also cites benefits gained from present installations.

Electric Welding

43-121—Automatic Voltage Compensator for Resistance-Welding Control; *E. M. Callender* (*A '35*), *R. S. Phair* (*A '34*). *20 cents.* The paper describes a simple electronic device that has been developed and used successfully to regulate the welding current under adverse line regulation. Line feeders are being overloaded heavily, and additional copper is often unavailable in this wartime emergency. The device, therefore, has special significance at this time. Circuits and general discussion of the welder control and the compensator are given. Numerical testing data are also given to show the importance of maintaining uniform welding current for consistent weld strength. Special interest may center around a brief discussion on methods of estimating instantaneous line voltage drops. This is just one of the problems in connection with resistance-welding regulation that is unique because of the application of phase-controlled ignition of the welding current.

43-93-ACO—Interim Report Emergency Measures to Increase Output of Generating Equipment and Systems; *AIEE Committee on power generation. 65 cents.* The AIEE standards committee, at its October 9, 1942 meeting, requested that each technical committee consider preparation of wartime guides to help the electrical industry find ways and means to "get more out of what it has." The power generation committee considered this assignment and decided that, because of many variables in construction and operation of generating stations and systems, they could best serve the industry by compiling a number of "hints" on this subject. The logical approach was to make a survey of what had been done by manufacturers and operating companies. Two subcommittees of the AIEE power generation committee have completed these assignments and this memorandum summarizes results of their work. One subcommittee, composed of manufacturers' representatives, assembled suggestions concerning generators. The other subcommittee, composed of operating-company representatives, obtained system data by means of a questionnaire which was quite widely distributed to the larger operating groups. Both of these subcommittee reports have been presented at the AIEE January technical conference in New York, the AIEE Kansas City technical meeting, and a joint meeting of the electrical equipment committee and prime movers committee of Edison Electric Institute. Many fine comments and suggestions received from these conferences have been incorporated in this final report.

Electrical Machinery

43-76—Pole-Face Loss in Solid-Rotor Turbine Generators; *W. W. Kuyper (A '34). 30 cents.* This paper presents practical methods of predetermining the pole-face losses in solid-rotor turbine generators. The fundamental mathematical analysis is shown in detail together with the steps taken in simplifying the general equations for the range of problems usually encountered by the designer. Sample calculations, together with test results, are included. To make the paper complete within itself as a method of determining these losses, methods of calculating winding factors are included. From the information presented, it is expected that designers will be able to predetermine pole-face losses for substantially all cases encountered with greater facility and accuracy than has heretofore been possible.

43-82—Critical-Material Conservation in Induction-Motor Manufacture; *Henry M. Hobart (F '12). 15 cents.* The War Production Board's general conservation order *L*-221 is stated to contain a provision whereby the horsepower of a new electric motor shall not exceed the requirements of the specific job for which the motor is purchased. Compliance with this and other conservation provisions contained in the order is expected to conserve annually about 6,000 metric tons of copper and 55,000 metric tons of carbon steel. The present paper directs attention specifically to the case of general-purpose induction motors and describes means by which the desired conservation of critical metal can be effected promptly and without any impairment of the service reliability of the induction motors or any shortening of their life below the term of years experienced with the heretofore extremely conservative basis of rating of all general-purpose motors. The plan (which employs capacitors), ensures a high stalling load, and improved power factor and efficiency at all loads, involves no loss of time for preparing new designs, and conserves a greater amount of critical material than the amount envisaged in WPB *L*-221.

43-83—A New Type of Adjustable-Speed Drive for A-C Systems—II. *A. G. Conrad (M '40), S. T. Smith (A '43), P. F. Ordung (A '43). 15 cents.* The preceding paper of this series presented the theory and principles of operation of a new type of a-c motor and its control circuit. This system of speed control has the advantage of being economical from the standpoints of initial cost, efficiency of operation, and ability to correct power factor. However, these advantages are not realized unless the machine is suitably designed and its control circuit properly adjusted to supply the necessary voltages and currents. The purpose of this paper is to explain the design proportions and the adjustments of the supply voltages to provide for optimum operating characteristics.

43-87—Interim Report on Overloading Distribution Transformers; *AIEE transformer subcommittee. 15 cents.* The "Interim Report on Guides for Overloading Transformers and Voltage Regulators," which was presented at the AIEE summer convention, June 22, 1942, and published in AIEE *Transactions*, volume 61, 1942, September section, pages 692–4, gave information on the overloading of all types of transformers and voltage regulators. It was recognized, however, that the information given in that report might be somewhat conservative for distribution transformers. For this reason an additional report is being issued for use in conjunction with the above-mentioned report but specifically applicable to the operation of distribution transformers.

43-88—High-Voltage Power-Transformer Design; *M. B. Mallett (M '37). 20 cents.* A core-type power-transformer design with shielded high-voltage winding and graded insulation has been developed for application in the higher-voltage classifications, and up to the present time more than 250,000-kva capacity of this kind of transformer has been manufactured and placed in service. The design employs a new form of construction for high-voltage power transformers which has been called "distributed concentric construction." Outstanding characteristics of the distributed concentric-type transformer include

1. Reduced inherent reactance resulting in a reduction of critical materials for a given specification.
2. Ready adaptability to forced oil flow directed through the winding ducts, resulting in a reduction of critical materials for a given thermal capacity.
3. Simple and stable mechanical construction of windings, insulation, and shield.
4. Strategic distribution of voltage stress throughout the dielectric structure.

43-89—Quadrature Synchronous Reactance of Salient-Pole Synchronous Machines; *R. V. Shepherd (A '36), C. E. Kilbourne (M '37). 20 cents.* Quadrature synchronous reactance is of prime importance in determining the synchronizing coefficient P_r, which must be known accurately when synchronous machines are combined with mechanical drives having pulsating shaft torques, such as are found in Diesel or gas engines and reciprocating compressors. This paper presents refinements in the theory and calculation of x_q by taking into account the presence of slots in the stator and rotor surfaces. It presents a discussion of four methods of testing for x_q, two of which have not previously been published, and compares results obtained by calculation and by the four test methods. Results show that x_q can be calculated accurately over the wide range of machine parameters encountered in modern design and that a new method of test, the maximum lagging-current method described in the paper, yields dependable and accurate values of x_q and has great advantages in simplicity of technique and ease of accomplishment over all other methods presented.

43-99—The Calculation of Unbalanced Magnetic Pull in Synchronous and Induction Motors; *R. C. Robinson (A '39). 20 cents.* There is outlined in this paper a method of calculation of unbalanced pull which takes into account the combined effects of saturation, parallels, and primary reactance. The increased accuracy which this method affords should make possible more accurate predictions of mechanical deflections and critical speeds.

43-100—Distribution Factors and Pitch Factors of the Harmonics of a Fractional-Slot Winding; *M. M. Liwschitz (M '39). 15 cents.* The investigation of the fractional-slot windings with respect to their harmonic (differential) leakage, to noise, and so forth, is handicapped by the lack of a simple formula for the distribution factor of their harmonics. The object of this paper is to derive for the distribution factor of the fractional-slot windings formulas similar to those of the integral-slot windings. For this purpose the usual way of studying the fractional-slot windings, that is, the use of charts, is abandoned. The vector diagram of the different slots is used instead.

43-114—Operation of Nonsalient Pole-Type Generators Supplying a Rectifier Load; *M. D. Ross (M '42), J. W. Batchelor (A '38). 15 cents.* This paper presents test results of temperature measurements on a nonsalient pole-type generator, when supplying a rectifier load, and presents a method of calculating losses caused by rectifier harmonic currents. Calculations substantiated by tests indicate that for most applications one may neglect the effect of the rectifier harmonic currents on the temperature rise of the generator.

Electrochemistry and Electrometallurgy

43-95 ACO—Arc-Furnace Control by Regulex Exciters; *T. B. Montgomery (M '41), T. H. Bloodworth (A '41). 15 cents.* Separately excited commercial d-c generators are inherently power amplifiers with amplification ratios of 20 to 50. When two such machines are connected in tandem, the total ratio is the product of the ratios of the individual machines. Special design permits further resultant amplification and fits the machines for their use as regulators. Such amplifiers when used to regulate an electrical quantity require stabilization to prevent oscillation. This type of system applied to arc-furnace control has been made inherently stable. The control system is set at equilibrium at a given value of arc watts. Unbalance due to variations is transmitted through the closed system of the amplifier, without contacts, quickly establishing a *proportional* corrective speed in the electrode motor. Oscillograms show that arc energy is held to an average value, which has increased the heating rate per unit of power expended under inherently rapidly fluctuating conditions.

43-96—Rotating Regulator for Arc Furnaces; *R. A. Geiselman, C. C. Levy (A' 33), W. R. Harris (A '38). 15 cents.* Increased production of steel alloy for the war effort has brought about a wider use of electric-arc furnaces, and with this improvements have been made in automatic regulation of electrode position. The regulator described in this paper is of the rotating type which controls electrode position as determined by response to current in the electrode and voltage between the electrode and the furnace shell. Features and performance of a typical installation are illustrated.

Electronics

43-90—Theory of Rectifier—D-C Motor Drive; *E. H. Vedder (M '35), K. P. Puchlowski (A '43). 30 cents.* This paper is an introduction to the theory involved when a d-c shunt-wound motor is being operated from a rectifier. The theory applying to a system composed of a single-phase full-wave rectifier and d-c motor is developed mathematically. The equations derived are plotted graphically, showing current wave shapes, torque, and speed characteristics as influenced by motor armature resistance and inductance and by a-c supply voltage.

43-113—The Testing of Mercury-Arc Rectifiers; *H. L. Kellogg (A '41), C. C. Herskind (M '40). 30 cents.* This paper describes the testing procedure used in the development and manufacture of mercury-arc rectifiers. The high standard of performance required of rectifier equipment can be attained only by careful and comprehensive testing to assure elimination of possible causes of trouble. The arc-back phenomena encountered in rectifiers necessitate special testing methods which are discussed at length. Data are also included showing the results of tests. It is hoped that the material presented may be useful in the development of rectifier standards.

Industrial Power Applications

43-115—Load Pickup by a Group of Ignitron Rectifiers; *L. W. Morton (A '38), D. I. Bohn (M '41). 15 cents.* This paper analyzes the problem of energizing a large electrochemical-cell-line load from a group of d-c conversion equipments such as mercury-arc rectifiers. A description of widely used methods is included. Field tests covering a new grid-control scheme for starting a group of ignitrons are reported. Advantages of the new scheme are set forth. Oscillographic illustrations showing the results of these tests are particularly interesting. These field tests were comprehensive and of practical value, because they are full scale, having been made in a large aluminum plant on a cell line in regular normal operation. During these tests it was possible also to investigate other interesting phenomena in connection with switching large electrochemical loads on and off. Oscillographic illustrations of transient behavior caused by these switching operations are shown and will be of interest to many engineers who had been unable to do more than speculate on the problems in the past.

43-116—Thyratron Motor Control; *E. E. Moyer, H. L. Palmer (A '42). 20 cents.* One of the earliest applications of electronic devices was to control d-c motors. Recent developments greatly expand the number of industrial uses. Electronic motor control supplies the armature with variable-voltage direct current from a grid-controlled thyratron rectifier and supplies the field from Phanotron tubes for constant field excitation or thyratrons for field weakening control. Vacuum tubes actuate the thyratrons' saturable-reactor phase-shift circuits in response to signals of compensated armature voltage, armature current, and field voltage. These signals are compared against a glow-tube voltage standard, amplified, and applied in correct sequence to give desired motor characteristics. Switching duty on armature-circuit reversing contactors is minimized by the current-interrupting and -initiating action of the thyratron tubes. Similar controls are applicable to motor-generator-set drives to control the fields of these machines in correct sequence to maintain preset speed, armature voltage, and loop current conditions.

Instruments and Measurements

43-81—A Capacitance Bridge for Determining the Ratio and Phase Angle of Potential Transformers; *H. W. Bousman (M '36), R. L. TenBroeck (A '29). 15 cents.* A capacitance bridge for the calibration of instrument potential transformers up to 300 kv is presented in this paper. This bridge provides measurements which are correct within 0.1 per cent on ratio and three minutes on phase angle. There are within the bridge arrangements for testing its accuracy as determined by the stability of its component arms. It is particularly desirable to have this means of self-testing if transformers are to be tested in a laboratory which does not have extensive standardizing facilities. Further advantages beyond accuracy for standardization purposes are flexibility and ease of operation for production testing and low cost and small volume as compared to a high-voltage resistance potentiometer.

Power Transmission and Distribution

43-77—The Effect of Corona on Coupling Factors Between Ground Wires and Phase Conductors; *G. D. McCann (A '38). 20 cents.* Measurements of coupling factors between ground wires and phase conductors as a function of ground-wire voltage have been made for a sufficient number of cases to determine them for any of the standard transmission-line configurations. It was found that the effect of corona causes the coupling factor to increase appreciably with ground-wire potential. This effect is greater for positive-polarity than for negative-polarity potential of the ground wire. A study was made to show the visual dimensions of the ground-wire corona sheath, as compared to the effective increase in the ground-wire radius from the standpoint of its effect upon the coupling factors. Tests were also made to determine the conditions of flashover between a ground wire and floating phase conductor. Data were obtained showing the comparison between the breakdown values of standard test gaps and transmission-line insulation under these conditions.

43-80—Application of Carrier to Power Lines; *F. M. Rives (M '41). 30 cents.* This paper deals with the general problem of forming carrier circuits on transmission lines, describing the characteristics and functions of the principal tools used and the practices that have been followed in applying them. A method of computing the principal attenuation components and total attenuation of typical carrier circuits is presented in sufficient detail to be useful to the power utility engineer having little or no experience with carrier apparatus.

43-85—Safe Ratings for Overhead Line Conductors; *Leonard M. Olmsted (M' 39). 30 cents.* Many factors must be considered in establishing safe ratings for overhead copper line conductors. Most important is the low-temperature annealing and loss of strength of the hard-drawn copper itself. Others are the effect of atmospheric conditions and line location relative to structures and topographical features tending to obstruct the wind. The actual construction details of the line, such as ground clearances, conductor splices, and connectors, and the policies of the personnel responsible for maintaining the line are pertinent. This paper attempts to show how these factors can be evaluated to establish operating ratings, indicates how readily available data can be utilized, and points out the lack of completely satisfactory data on other points. Procedure is stressed rather than results, as the influence of local conditions upon many of the factors precludes publication of ratings which are entirely safe to apply without careful investigation of the effect upon the copper conductors.

43-92—Distance Relay Protection for Subtransmission Lines Made Economical; *L. J. Audlin (M '35), A. R. van C. Warrington (A '31). 15 cents.* Less important transmission lines are usually protected with directional overcurrent relays. Much better protection and consequently improved service would be provided by modern high-speed distance relays but at several times the cost. What is required is a distance-relay arrangement considerably less expensive than the relays now used on high-voltage transmission lines but with as nearly as possible the same characteristics. Such an arrangement is now available. The paper describes successful tests in the laboratory and on an actual system in this country with both natural and staged faults. The equipment consists of a single high-speed reactance relay which is enabled to protect all three phases by being automatically switched to the correct potential and current by a phase-selector relay. Compared with directional overcurrent relays for phase faults, the new scheme is easier to apply and gives complete selectivity, combined with high-speed tripping, at only a moderate increase in cost. Compared with conventional distance relays, the new scheme effects a saving in critical materials and labor, since one distance relay and an auxiliary relay replace three distance relays. This in turn reduces investment, panel space, and maintenance.

43-97—A Study of Voltage Transients in Arc-Furnace Circuits; *J. B. Hodtum (M '26), J. B. Rice (A '38). 25 cents.* In the history of the operation of arc furnaces there have been evidences of high-voltage surges. These have occasionally resulted in failure of associated electrical equipment. In the last three years an increasing amount of attention has been focused on this problem, chiefly because of the increasing number of arc-furnace installations and the seriousness of interruption to production. Following a general discussion of arc-furnace operating procedure and possible causes of voltage surges in furnace circuits, the paper presents the results of voltage surge measurements made on two typical arc furnace installations. The results of magnetic and cathode-ray oscillograms and of Klydonograph surge measurements are presented. Emphasis in the tests was placed on determining the source of voltage surges.

43-98—Practical Calculation of Electrical Transients on Power Systems; *R. D. Evans (F '40), R. L. Witzke (A '37). 25 cents.* This paper is devoted to the practical calculation of power-system transients and presents concepts, methods, and approximations selected with that objective in view. A method of calculation adapted to engineering work, designated as a "method of zeros and slopes," is described and illustrated by selected examples. Consideration is given to the physical interpretation of transients, particularly with the aid of the properties of equivalent circuits. The presentation is from the standpoint of the electrical engineer, who is accustomed to making steady-state calculations, but who has not given special study to the transient problem required by the mathematical approach generally used.

43-104—Guide for Wartime Conductor Temperatures for Power Cables in Service; *Association of Edison Illuminating Companies subcommittee on wartime temperatures for cable circuits. 20 cents.* This guide was prepared to aid in obtaining maximum practical utilization of underground power-cable circuits during wartime and still have tolerable rates of failure. Tables are given for recommended maximum conductor temperatures for normal daily operation and for occasional emergency operation for cables with the following insulations: impregnated paper of solid and oil-filled types, varnished cambric, and rubber. Factors affecting allowable loading and temperatures are discussed to indicate variations from the temperatures in the tables that should be made for given conditions and to indicate methods of obtaining maximum utilization with the temperatures set. All of the temperatures are substantially above previous standard temperatures and are recommended for use only in wartime to save critical materials, because their use is otherwise uneconomical and somewhat damaging to service reliability.

Power Transmission

43-105—Interim Report on Emergency Overloads on Overhead Conductors; *working group on towers, poles, and conductors of AIEE transmission subcommittee. 15 cents.* Present conditions in many cases make it necessary to establish new load limits for overhead conductors. This paper calls attention to the principal factors involved and the wide variation in available information on this subject, particularly in respect to annealing characteristics of conductors materials. Some factors affecting annealing are unknown and need to be investigated. Weather records should be used with caution and applied with full knowledge of conditions along the entire line. The effect on joints and possible heating of clamps or other magnetic fittings encircling the conductor are touched on. The report cautions against the use of overoptimistic values, resulting in serious damage to the conductors.

43-112—Frequency Modulation for Power-Line Carrier Current; *E. W. Kenefake. 15 cents.* The developments of frequency modulation in the low-frequency range below 200 kilocycles have indicated certain advantages for its use in power-line carrier. This paper discusses developments in the use of frequency modulation with a one-to-one deviation ratio for carrier-current applications. The basic characteristics of noise reduction by means of frequency modulation are discussed, and the results of various measurements with different types of interference are presented. These results point out that any interference arising from undesired amplitude modulation of the carrier is greatly reduced when frequency modulation is used. Since that type of interference is sometimes encountered on power lines, frequency modulation can be used to advantage in those cases. By the use of a one-to-one deviation ratio it is shown that the band width of a frequency-modulated channel can be made the same as in an audio-frequency modulated channel. This overcomes many difficult application problems.

Protective Devices

43-79—Pilot-Wire Relaying on a Metropolitan System; *T. G. LeClair (F '40), E. L. Michelson (A '37). 20 cents.* The inherent selectivity of pilot-wire relaying makes it an ideal method for protecting transmission lines. This paper describes the pilot-wire systems employed on a large metropolitan power system where this type of relaying is used extensively. Three different arrangements are used, all of which are described in the paper. All schemes are similar in principle and use circulating a-c currents to make a direct comparison between the currents at the two ends of the main transmission line. This paper shows the net effect of pilot-wire troubles on relay operation. It points out that induced voltages on pilot wires are not a serious problem on an underground system. It also gives an analysis of relay operations for a ten-year period which indicates a relatively good record for pilot-wire relaying. It shows that pilot-wire relaying is more reliable than other types of high-speed relays. The paper discusses the relative merits of owned pilot wires versus leased telephone wires. Owned wires have a higher cost but offer several operating advantages. It compares pilot-wire relaying with carrier-current relaying. Pilot-wire relaying can be applied economically only on relatively short transmission lines, but, where applicable, it provides a more reliable form of protection than carrier-current relaying.

43-91—Carrier-Current Differential Protection for Transformer Banks; *T. A. Cramer (A '41). 15 cents.* Under present conditions where new manufacturing plants must be served with as little additional equipment as possible, the power-system designing engineer frequently finds it expedient to install a transformer with only a low-voltage circuit-breaker. This arrangement presents a problem for the protection engineer since the reactance of the transformer bank makes it difficult for the remote relays to recognize faults in the low-tension winding. This paper describes a method of completing the protection of the transformer bank by adding an audio-frequency modulating signal to the standard carrier-current pilot-relaying channel to trip the remote circuit breaker when the transformer differential relay operates.

43-102—The Cause and Control of Some Types of Switching Surges; *T. W. Schroeder (A '37). 15 cents.* Switching-surge overvoltages, particularly those resulting from interrupter restriking when opening the normal balanced charging currents of cable circuits, long high-voltage lines, or large capacitor banks, recently have received attention in the technical literature. This paper presents the results of analyses of other methods of reducing the possibility and magnitude of these overvoltages, namely, the dependence on saturable transformer windings in parallel with the capacitance to discharge it following the first interruption. The effect of transformer size relative to the capacitance and the method of transformer connection are discussed. "Low-side" switching of lines and cables is indicated to be very effective. A summary of the capacitive-circuit switching problem and its solution by the use of resistor-shunted interrupters is given. In addition, an analysis is made of the problem of off-current-zero interruption of transformer magnetizing current. This analysis indicates the amount of capacitance required in shunt with the transformer (and switched with it) to hold the voltage resulting from an interruption at the crest of the current wave to any allowable value.

43-103—A New High-Interrupting-Capacity Fuse for Voltages Through 138 Kv; *H. H. Fahnoe* (*A '35*). *15 cents.* The reliable performance of high-interrupting-capacity fuses in the intermediate-voltage field has increased interest in the application of such fuses for higher voltages. A new fuse has been developed to meet the requirements for service on important transmission circuits operating at 44 to 138 kv. The requirements for this service are discussed along with other factors that prevent such a fuse from being a mere enlargement of lower-voltage fuses. A description is given of the new boric-acid fuse which, for its interrupting capacity, is light in weight, permitting the dropout design necessary at these higher voltages. The data and oscillograms presented cover high-power laboratory tests under all possible fault conditions and assure reliable short-circuit protection on systems capable of delivering three-phase symmetrical short circuits of 1,000,000 kva.

43-110—A Simple Method for the Determination of Bushing-Current-Transformer Characteristics; *Stuart D. Moreton* (*A '41*). *20 cents.* In line with the present trend toward the calculation of bushing-current-transformer characteristics from typical excitation curve data, this paper presents an analysis of the errors introduced by the simple method of "inphase" addition of exciting current and secondary current. These errors are compared with the expected variation in ratio correction factor due to variation of the parameters, especially the iron core. The conclusion is reached that this simple method gives results that are not only well within this expected variation but also well within engineering accuracy. An extension of this method is presented for the determination of phase-angle errors with a little extra effort. Inphase addition is expanded by a graphical process, so that only one point need be calculated to draw a complete characteristic curve. This is performed by means of one template that is good for all burdens, ratios, and voltage ratings. A special co-ordinate paper is presented that gives the ratio correction factor in terms of either the per cent rated primary current or the per cent rated secondary current. It has the advantage of giving the secondary current directly in terms of the primary current.

43-111—Report on Application of Lightning Protective Devices in Wartime; *AIEE lightning arrester subcommittee. 15 cents.* This report by the lightning arrester subcommittee has been prepared to suggest ways and means of applying protective devices such as lightning arresters in such a manner as to save critical materials either directly or indirectly. Eight suggestions for accomplishing this are presented and discussed. These are:

1. Applying arresters on basis of voltage rating.
2. Rebuilding and revamping old arresters.
3. Short connections between arresters and equipment.
4. Line-type arresters in place of station-type
5. Protection of equipment not already protected.
6. Protective devices other than arresters.
7. Shielding of stations
8. Tests and maintenance of arresters.

A careful study of the problem of supplying overvoltage protection, particularly by the use of lightning arresters, should yield considerable over-all benefits, not only in saving of critical materials at the present time, but also in reducing damage to expensive and hard-to-replace equipment, as well as improving continuity of service to power systems.

43-117—A New High-Speed Balanced-Current Relay; *V. N. Stewart* (*A '38*). *15 cents.* A high-speed induction-cylinder-type relay has been developed in which the operating-coil circuit has an inherent delay of about a cycle, because of the time required to build up current in a resonant circuit. The restraining coils of the relays are lagged with copper rings and produce restraint in a small fraction of a cycle. Complete description of operating principles and tests are covered by this paper.

43-118—Relay Protection for Three Terminal Lines; *M. A. Bostwick* (*A '42*), *E. L. Harder* (*M '41*). *15 cents.* The war, with its attendant shortage of critical materials, has led to a considerable increase in the number of tapped lines. This paper deals with the method of protecting circuits of this kind under three headings:

1. Pilot-wire protection, a-c and d-c schemes.
2. Carrier-current protection.
3. Other protective schemes.
Directional overcurrent or network.
Distance type.

Three terminal lines are classified into three types with regard to other system connections between the terminals connected by the line in question. These are classified further as to power sources to aid in analyzing limiting conditions for relay applications. Typical operating characteristics of a-c pilot-wire equipment for three terminal lines are shown. In general, in dealing with pilot-wire carrier, and other types of protection, those problems peculiar to application on three terminal lines are analyzed, and methods are given for solving some of the more troublesome problems.

43-119—Interim Report on Application and Operation of Out-of-Step Protection; *AIEE working group of relay subcommittee. 20 cents.* Out-of-step protection of synchronous machines and system interconnections is now demanding serious consideration because

1. The heavy loading of machines and transmission lines have introduced many stability problems.
2. Outages due to out-of-step conditions may interfere seriously with the war effort.

The relay subcommittee has, therefore, considered it timely to prepare a report on the subject and has appointed a working group for that purpose. The report describes briefly the phenomena of out of step, the methods of protection available, and a cross section of the practices and requirements of representative utility systems.

Safety

43-94 ACO—Practical Accident Prevention in Small Groups; *O. S. Hockaday* (*M '28*). *15 cents.* The stress of war production places on industrial management a keener responsibility for preventing accidents. Large corporations usually have safety departments to direct this effort, but scattered over the country are thousands of industrial concerns of modest proportions which are not in position to set up formal safety organizations. Suggestions are offered in this paper as to how the superintendent of a small group may develop within his existing personnel an effective safety program.

Transportation

43-78—Adequate Electrical Maintenance Essential to Transportation; *W. J. Clardy* (*M '39*). *15 cents.* The demand for public transportation is increasing rapidly under present conditions of accelerated industrial activity. Adequate maintenance of electrical apparatus on rolling stock is essential to avoid impairment to the war effort. Measures which will help to retain generators, motors, and control in service practically 100 per cent of the time are proper operation, effective inspection, preventive maintenance, keeping apparatus clean, accurate adjustments, prompt worn part replacements, undelayed commutator conditioning, use of improved insulation, and correct lubrication. More careful attention to all of these factors during the present emergency will assure maximum rolling-stock availability.

PERSONAL ● ● ● ●

C. H. Sanderson (A '07, F '18) system planning engineer, Consolidated Edison Company of New York (N. Y.), Inc., has retired. Mr. Sanderson who was born in 1880 commenced his engineering career in 1901 with a year of shopwork at the Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa., after which he was transferred to the drafting department. In 1906 he did switchboard and power-station design and in 1919 was given supervision of this work for the eastern United States and all foreign trade. As general engineer from 1911 to 1913 he assumed the duties of commercial engineer salesman. He worked as electrical engineer for the Havana (Cuba) Electric Railway, Light, and Power Company from 1913 to 1917. From 1918 to 1920 he was connected with Frank G. Robbins Company, Havana. His association with the New York Edison Company dates from 1921 when he took the position of assistant electrical engineer and assistant system engineer. In 1937 he was made system planning engineer. He has written and edited books and is editor in chief of "American Standard Definitions of Electrical Terms." He is also a member of the Edison Electric Institute, the Professional Electrical Engineers, the American Standards Association, and the United States Chamber of Commerce.

J. H. Gardner (A '25, M '34) formerly lieutenant colonel, Signal Corps, United States Army, and director of the Aircraft Laboratory, Wright Field, Dayton, Ohio, was among the 52 temporary brigadier generals recently appointed by President Roosevelt. He holds the degrees of bachelor of science (1913) and master of science (1915) from Union College and the degree of master of science in electrical engineering (1924) from Yale University. Before joining the army he was employed by the General Electric Company, Schenectady, N. Y., and Chicago, Ill., from 1913 to 1917. He served as radio and telephone officer in the Field Artillery from 1917 to 1919. From 1919 to 1921 he was with the Intelligence Section of the army of occupation in Germany. In 1921 he was transferred to the Signal Corps and in succession became assistant to the signal officer, Fifth Corps Area, Columbus, Ohio, in 1925, executive officer for research and engineering in the office of the chief signal officer, Washington, D. C., in 1926, executive officer of the Signal Corps Laboratories, Fort Monmouth, N. J., in 1931, and executive officer of the Aircraft Laboratory, Dayton in 1939. He was made director of the laboratory in 1940. In 1935 he was promoted from the rank of captain to major and in 1941 to lieutenant colonel.

H. Y. Hall (A '03, F '27) production superintendent, Consolidated Edison Company of New York (N. Y.), Inc., recently retired. Mr. Hall received the degree of bachelor of science in electrical and mechanical engineering from the Alabama Polytechnic Institute in 1900. He immediately entered the testing department of the General Electric Company, Schenectady, N. Y. In 1905 he received the degree of electrical engineer. He was employed in the operation and testing department of the Manhattan Railway Company, New York, N. Y., from 1902 to 1906. After a year as construction foreman for J. G. White Company, New York, N. Y., he joined the Southern Pacific Railroad Company, San Francisco, Calif., in 1907 and remained with that company until he established his own consulting engineering practice in San Francisco in 1914. From 1917 to 1919 he worked as design engineer for the Neuelectric Transmission Company, Cleveland, Ohio, and for J. Livingston Company, Cleveland, from 1919 to 1921. He entered the employ of the United Electric Light and Power Company in 1921 as superintendent of their Hell Gate Station. In 1937 he became superintendent of their Waterside Station and in 1942 production superintendent.

W. H. Matthies (M '26, F '29) switching development director, Bell Telephone Laboratories, New York, N. Y., has retired. Born in Hamburg, Germany, in 1879, he graduated from the Massachusetts Institute of Technology in 1902. In the latter year he joined the Western Electric Company, New York, N. Y., as student engineer and for two years following held the position of equipment engineer. He was made chief engineer of the company's Berlin, Germany, branch in 1905. In 1911 he was transferred to Antwerp, Belgium, to the staff of the European chief engineer. There he supervised the work of the London, Antwerp, Paris, and Berlin branches. From 1912 to 1916 he specialized in machine switching development and in 1916 took charge of the machine switching laboratory in New York. In 1935 he was named switching development director of Bell Laboratories. He holds numerous patents and has been active in the development of the panel and crossbar systems and dial operation.

H. B. Leach (A '18) assistant superintendent, Consolidated Edison Company of New York (N. Y.) Inc., has retired. Mr. Leach, who was born at Wilmington, Del., August 12, 1886, had completed 31 years with the Edison company and its predecessors. He entered the employ of the New York Edison Company in 1913 after five years (1905–10) with the Wilmington (Del.) City Electric Company and three years (1910–13) with the Westchester Lighting Company, Mount Vernon, N. Y. Until 1918 when he was transferred to the test department he worked in the meter department of the Edison company. In 1920 he was made chief tester and in 1923 became foreman of the test division of the United Electric Light and Power Company. He was named general foreman of the meter and test department in 1930. Appointed assistant superintendent of the New York Edison Company in 1935 he continued in that position with the Consolidated Edison Company after the 1936 merger of the New York companies.

R. S. Elberty (A '25, M '35) formerly electrical engineer with the Landis Tool Company, Waynesboro, Pa., has opened his own consulting engineering office at Fort Lauderdale, Fla. Mr. Elberty received the degree of electrical engineer in 1925, and the degree of bachelor of science in commerce in 1929 from the University of Cincinnati. From 1925 to 1929 he worked as electrical engineer for the American Laundry Company, Cincinnati, Ohio. After a year as chief engineer with B. A. Wesche Electric Company, Cincinnati, he became associated as general engineer with the Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa., from 1929 to 1937. In the latter year he joined the Landis company.

J. E. Hayes (A '03) formerly president of the New Jersey Zinc Company, New York, N. Y., has been elected chairman of the board of that company. He has been president of the company since 1927. In addition he holds the position of president of the New Jersey Zinc Sales Company, the Empire Zinc Company, the New Jersey Zinc Company of Pennsylvania, Palmerton, the Palmerton (Pa.) Lighting Company, Palmerton Water Company, the Palmerton Telephone Company, the Chestnut Ridge Railroad Company, and the Saucon Valley Iron and Railroad Company. He received the degree of civil engineer in 1895 and the degree of electrical engineer in 1897 from Princeton University.

G. W. Pierce (A '11) professor of physics and communication engineering, Harvard University, Cambridge, Mass., recently was awarded a Franklin Medal and a certificate of honorary membership to the Franklin Institute. Doctor Pierce was honored for his development of "electrically squeezed" quartz crystals used to narrow the range of radio frequencies. He has been teaching at Harvard University since 1907 when he joined the faculty as assistant professor of physics. In 1920 he was the recipient of the medal of the Institute of Radio Engineers for distinguished service, to radio engineering.

G. B. Kersey (A '14, M '26) supervising field engineer, Commonwealth Edison Company, Chicago, Ill., has retired. Born May 24, 1883, in Lebanon, Ind., he was graduated from Purdue University with the degree of bachelor of science in electrical engineering in 1905. In the latter year, as a substation operator, he commenced a 38-year association with the Edison company. He was transferred to the engineering department as draftsman in 1907. Subsequently he became head draftsman and designing engineer. In 1922 he was made field engineer and in 1932 supervising field engineer.

W. E. Poor (A '15) executive vice-president Hygrade Sylvania Corporation has been elected president of Sylvania Products, Inc. Mr. Poor was assistant manager of the Hygrade Lamp Company, Salem, Mass., from 1909 to 1922, when he became chief engineer. In 1930 when his company combined with Sylvania Products Company he was appointed vice-president in charge of manufacturing. He was made executive vice-president of Hygrade Sylvania Corporation in 1940.

C. A. Butcher (M '22) formerly assistant manager of the district manufacture and repair department, Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa., has been made manager of that department for the Pacific Coast district of the Westinghouse company, Emeryville, Calif. Mr. Butcher has been assistant to the national manager since 1941. From 1929 to 1936 he was eastern engineering manager and eastern service manager from 1936 to 1941.

J. E. O'Brien (A '29 M '39) has been appointed a major, United States Marine Corps. Major O'Brien was formerly head of the Special Problems Section, Technical Standards Division, Rural Electrification Administration. Prior to his connection with the Rural Electrification Administration, he was a member of the electrical engineering staff of the Catholic University, Washington, D. C.

D. C. Jackson, Jr. (A '23, F '30) on leave of absence from his position as dean of the school of engineering, University of Notre Dame (Ind.) has been promoted from the rank of major in the United States Army to that of lieutenant colonel, Frankford Arsenal, Philadelphia, Pa. Lieutenant Colonel Jackson served as an officer in the Coast Artillery from 1917 to 1919.

E. J. Van Deusen (A '35,) M '43 assistant electrical engineer for the Mason and Hanger Company in charge of outside electrical distribution construction, Badger Ordnance Works, Baraboo, Wis., has been commissioned as lieutenant in the United States Naval Reserve. He has been on leave of absence from the Wisconsin Power and Light Company, Madison, for the past year.

H. S. Osborne (A '10, F '21) formerly assistant chief engineer, operations and engineering department, American Telephone and Telegraph Company, New York, N. Y., has been appointed chief engineer of that department. Mr. Osborne is currently serving as AIEE president. For a fuller biography see *Electrical Engineering*, January 1943, page 35.

G. W. Bower (A '18, M '40) formerly assistant to the Camden district superintendent has been appointed engineer in the general office, Public Service Electric and Gas Company, Newark, N. J. Mr. Bower has been with the Public Service company since he graduated from Drexel Institute of Technology in 1914.

J. K. Johnson (A '28) formerly engineer in charge of Chicago (Ill.) laboratory, Hazeltine Electronics Corporation, has become a special representative in the Office of Procurement and Material of the Office of the Under Secretary of the Navy, Washington, D. C. He had been with the Hazeltine corporation since 1934.

J. M. Standring (A '40) formerly senior staff engineer with the Bell Telephone Company of Pennsylvania, Philadelphia, has been transferred to the transmission group in the operations and engineering department of the American Telephone and Telegraph Company, New York, N. Y.

K. A. Ganssle (A '40) formerly engineer, general engineering department, Southern California Telephone Company, Los Angeles, has been transferred to the equipment and building section of the plant engineering division, American Telephone and Telegraph Company, New York, N. Y.

W. G. Steinbright (A '40) formerly assistant to the general superintendent of the suburban transmission and distribution division of the Philadelphia (Pa.) Electric Company has been appointed superintendent of the Schuylkill division of that company.

J. F. Atkinson (A '41) is now a lieutenant in the Signal Corps, United States Army, Galveston, Tex. Mr. Atkinson is on leave from the Technical Standards Division, Rural Electrification Administration, where he is an associate engineer.

C. H. Weaver (A '37) formerly commercial engineer in the marine sales department, Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa., has been appointed manager of the marine section. He has been with the Westinghouse company since 1936.

J. E. H. Stevenot (A '13, M '17) formerly vice-president and general manager of the Philippine Long Distance Telephone Company, Manila, is now a colonel in the United States Signal Corps and deputy chief of staff of General Douglas MacArthur.

J. B. Rees (M '31, F '32) equipment and building engineer, operations and engineering department, American Telephone and Telegraph Company, New York, N. Y., has been appointed to the Telegraph Operations Industry Advisory Committee of the War Production Board.

C. R. Camp (A '20, M '25) formerly field engineer, Commonwealth Edison Company Chicago, Ill., has been appointed supervising field engineer. He has been with the Edison company since 1915 as generating station operator, head draftsman, and, since 1927, as field engineer.

D. E. Basler (A '39) has been promoted to Captain, Signal Corps, United States Army, Washington, D. C. Captain Basler is on leave from his position as associate engineer, Technical Standards Division, Rural Electrification Administration.

J. W. Carpenter (A '40) formerly associate engineer, Technical Standards Division, Rural Electrification Administration, has been called to active duty. He holds a commission as a lieutenant, junior grade, in the United States Naval Reserve.

D. G. Housley (A '41) is now serving as a lieutenant, Army Air Forces Boca Raton Field, Fla. He is on leave from the Technical Standards Division, Rural Electrification Administration, where he is an assistant engineer.

W. A. Lewis (A '42) an ensign in the United States Naval Reserve, has been called to active duty. He is an assistant engineer on leave from the Technical Standards Division, Rural Electrification Administration.

N. W. Matthews (A '39) is now a second lieutenant, United States Marine Corps, Camp Murphy, Fla. Formerly, he was an associate engineer, Technical Standards Division, Rural Electrification Administration.

B. O. Watkins (A '43) has been commissioned a lieutenant, junior grade, in the United States Naval Reserve. Lieutenant Watkins was head of the Operating Problems Unit, Technical Standards Division, Rural Electrification Administration.

W. L. Cisler (M '35) assistant chief engineer, electrical engineering department, Public Service Electric and Gas Company, Newark, N. J., is chief of equipment production in the power section of the Office of War Utilities.

Howard Dingle (A '07, M '41) president of the Cleveland Worm and Gear Company, has been named to newly formed Gear and Speed Reducer Industry Advisory Committee of the War Production Board.

N. B. Hinson (A '19, F '35) chief engineer, Southern California Edison Company, Ltd., has been elected chairman of the Electric Power Interchange Committee which was formed to meet California's demands for power in war production.

A. W. Robertson (A '27) chairman of the board, Westinghouse Electric and Manufacturing Company, Pittsburgh, Pa., has been awarded the honorary degree of doctor of engineering by Stevens Institute of Technology.

F. W. Petit (A '23, M '37) formerly assistant engineer, Consolidated Edison Company of New York (N. Y.), Inc., is now employed by the Columbia University National Defense Laboratories, New York, N. Y.

J. T. Barron (A '07, F '27) vice-president in charge of electric operation, Public Service Electric and Gas Company, Newark, N. J., has been elected to a three-year term on the board of directors of the American Standards Association.

L. C. Larson (A '23, M '29) professor of electrical engineering, University of Wisconsin, Madison, has been appointed chairman of the Madison Section of the AIEE to fill the unexpired term of E. J. Van Deusen (A '35).

J. R. Oberholtzer (A '39) Associate Engineer, Technical Standards Division, Rural Electrification Administration, has been commissioned a second lieutenant in the Signal Corps, United States Army.

K. J. Plucknett (A '39) associate engineer, Technical Standards Division, Rural Electrification Administration, has been commissioned an Ensign in the United States Naval Reserve, Brighton, Mass.

W. T. Smith (A '40) associate engineer, Technical Standards Division, Rural Electrification Administration, has been commissioned a lieutenant in the United States Marine Corps.

J. L. Thurston (A '41), associate engineer, Technical Standards Division, Rural Electrification Administration, is now a Lieutenant in the Signal Corps, United States Army, Camp Maxey, Tex.

J. E. Dingman (M '37) division plant superintendent, American Telephone and Telegraph Company, New York, N. Y., has been appointed employees' relation manager in the long lines department.

P. V. Plummer (A '33) engineer with the United Illuminating Company, New Haven, Conn., has been made superintendent of electrical construction and maintenance in the production department.

O. E. Anderson (A '29) formerly staff assistant to the superintendent of construction, Commonwealth Edison Company, Chicago, Ill., has been made assistant superintendent of construction.

N. K. Davis (A '40) chief engineer of the Georgia Public Service Commission, Atlanta, is chief of the allocations section of the power division of the Office of War Utilities.

J. E. Moore (A '12) consulting engineer, Ebasco Services, Inc., New York, N. Y., is chief of the power supply section of the power division of the Office of War Utilities.

Norman Knight (M '37) chief engineer of the western area of the Bell Telephone Company of Canada, Toronto, Ont., has been elected president of the Electric Club of Toronto.

W. E. Melarkey (A '27) formerly executive engineer of The Hilo (Hawaii) Electric Light Company, is now lieutenant commander in the 14th Naval District at Pearl Harbor, Hawaii.

Edward Falck (A '40) special assistant to the vice-president, Consolidated Edison Company of New York (N. Y.), Inc., is deputy director of the Office of War Utilities.

H. J. Billica (A '36, M '36) formerly with the Office of War Utilities, War Production Board, Washington, D. C., is now employed in the Washington office of the Copperweld Steel Corporation.

J. G. Harden (A '34) toll plant chief, Indiana Bell Telephone Company, Indianapolis, has been transferred to Bell Laboratories, Inc., New York, N. Y., for the duration of the war.

Wilfred Sykes (A '09, F '14) president of the Inland Steel Company, Chicago, Ill., has been awarded the honorary degree of doctor of engineering by the Illinois Institute of Technology.

C. C. Johnson (M '20) president of the American District Telegraph Company, New York, N. Y., has been nominated a vice-president of the National Fire Protection Association for 1943.

A. R. Small (M '37) president of the Underwriters' Laboratories, Inc., Chicago, Ill., has been elected to a three-year term on the board of directors of the American Standards Association.

W. L. Crosby (A '41) formerly foreman in the test bureau of the Consolidated Edison Company, New York, N. Y., has joined the Submarine Signal Company, New York, N. Y.

W. C. White (A '23, M '30) director of the electronics laboratory, General Electric Company, Schenectady, N. Y., has been named to the board of directors of the Institute of Radio Engineers.

J. J. Ryan (M '23) general superintendent, meter bureau, Consolidated Edison Company, New York, N. Y., has been elected president of the Edison Savings and Loan Association.

Vannevar Bush (A '15, F '24) president of the Carnegie Institution of Washington (D. C.), has been awarded the honorary degree of doctor of science by Stevens Institute of Technology.

E. O. Shreve (A '06) vice-president, General Electric Company, Schenectady, N. Y., was recently elected a member of the board of directors of the United States Chamber of Commerce.

D. J. O'Conor, Jr. (A '38) has been appointed assistant chief engineer of the Formica Insulation Company, Cincinnati, Ohio. He has been employed by the Formica company since 1940.

R. L. Morris (M '31) assistant engineer, Consolidated Edison Company of New York (N. Y.), Inc., has received a wartime leave of absence to join the Tel Autograph Corporation, New York, N. Y.

L. A. Nettleton (A '21, M '26) formerly assistant engineer, Consolidated Edison Company of New York (N. Y.), Inc., has joined the Massachusetts Institute of Technology, Cambridge, Mass.

H. S. Orcutt (A '26) formerly assistant engineer, Consolidated Edison Company of New York (N. Y.), Inc., is now with the Federal Radio and Telephone Company, East Newark, N. J.

Norman Franks (A '41) manager of the Toronto district, Canadian General Electric Company, Ltd., Toronto, has been elected president of the Electric Service League of Toronto.

H. W. King (A '37) formerly assistant engineer, Consolidated Edison Company of New York, Inc., is now with the Anaconda Wire and Cable Company, Hastings, N. Y.

C. E. Dean (A '23, M '37) radio engineer, Hazeltine Service Corporation, New York, N. Y., has been elected vice-president of the Radio Club of America.

L. E. Packard (A '40, M '41) engineer, General Radio Company, New York, N. Y., has been elected recording secretary of the Radio Club of America.

OBITUARY • • • •

Philip Herd (M '21) partner in Wilson and Herd, Ltd., Johannesburg, Union of South Africa, died on August 19, 1942. Born November 12, 1870, at Birkenhead, Cheshire, England, he received his early electrical training as apprentice electrical engineer for the Telegraph Manufacturing Company, Ltd. (now British Insulated Cables, Ltd.), Helsby, Cheshire, England, from 1886 to 1889. From 1890 to 1896 he was employed successively by the White Star and the Dominion Steamship Lines, and the Liverpool (England) Electric Overhead Railway. In 1897 and 1898 he engaged in insulating and operating gold mine electrical plants in South Africa and at electrical construction in Beird, Portuguese East Africa. Joining the staff of the South African General Electric Company, Ltd., Johannesburg, in 1899, he entered the test course in the foreign engineering department of the General Electric Company, Schenectady, N. Y. In 1900 he returned to South Africa and remained in the service of the General Electric Company as engineer and manager for Cape Colony, Union of South Africa, until 1916. In the latter year he became a member of the engineering firm of F. Peabody Rice and Wilson. He had been managing director of this firm since 1916.

Robert Anderson Carter, Jr. (A '09) manager of gas production, Consolidated Edison Company of New York (N. Y.) Inc., died April 2, 1943. Born August 9, 1884, in Brooklyn, N. Y., he entered the employ of the Consolidated Gas Company, New York, N. Y., in 1902. After a year and a half in various training positions, he was made assistant engineer in 1903, and a cadet engineer in 1904. He became assistant engineer with the Central Union Gas Company, New York, N. Y., in 1905. In 1908 he joined the Westchester Lighting Company, New Rochelle, N. Y., as cadet engineer. From 1922 to 1928 he was engineer of manufacture for the Consolidated Gas Company and the Astoria (N. Y.) Light, Heat and Power Company. He was named vice-president of the Astoria company in 1928. When that company merged into the Consolidated Edison company in 1936 he was appointed engineer and later manager of gas production for the Consolidated Edison company. He was the author of several technical papers and a member of the American Gas Association, the American Society of Mechanical Engineers, the Illuminating Engineering Society the Society of Gas Engineering, and the Society of Gas Lighting.

Francis Ogden Blackwell (A '00, M '03) president of Viele, Blackwell and Buck, New York, N. Y., died on April 11, 1943. Mr. Blackwell was born June 23, 1868, in New York and received the degree of civil engineer from Princeton University in 1887. During 1887 and 1890 he worked for the Bentley-Knight Electric Railway Company, New York, N. Y. In 1890 he joined the Thomson Houston Electric Company, Lynn, Mass., as assistant engineer of the railway division. When that company became part of the General Electric Company, Schenectady, N. Y., in 1892 he was made chief engineer in the power and mining department. As such he was responsible for that company's development of long-distance power transmission. In addition he built mining plants in California, Oregon, Utah, Minnesota, Louisiana, and Mexico. He resigned in 1906 to form his own consulting engineering firm in New York, N. Y. During World War I he served as consulting engineer to the United States Navy and War Departments. More recently he was engaged as consultant on the Muscle Shoals project and on the design of the St. Lawrence Waterway.

John Henry Schroder (M '42) Lieutenant commander, United States Navy, San Diego, Calif., died May 3, 1943. Born December 1, 1899, in Walhalla, S. C., he received the bachelor of science degree in mechanical engineering from Clemson College in 1921. From 1918 to 1920 he served in the United States Army. He commenced his engineering career in the construction division of the electrical engineering department of the Public Service Production Company, Newark, N. J., from 1921 to 1923 when he became an engineering assistant. From 1925 to 1927 he worked on design and purchase of materials for the Public Service Gas and Electric Company. In 1928 he joined the United Engineers and Constructors, Newark, N. J., where he designed switching tations until 1936. Returning to the Public Service Gas and Electric Company in 1936 he was assigned as sponsor engineer doing rebuilding and installation for switching and generating stations. In 1942 he received a leave of absence for the duration of the war to serve in the Navy.

Henry Clay Hart (M '27) vice-president of the Cuban Telephone Company, Havana, died on March 23, 1943. Mr. Hart, who was born January 22, 1881, in Jacksonville, Fla., entered engineering as a draftsman with the United States Government of Intervention in Cuba in 1901. During 1903 and 1904 he supervised a surveying party for the Bahia Honda (Cuba) Land and Improvement Company. He had charge of a surveying corps as well as being resident engineer with the Havana (Cuba) Central Railway in 1905 and 1906. He held the positions of engineer and general superintendent of the Electricity and Traction Company of Santiago, Cuba, in 1907, district engineer with the Department of Public Work, Santiago, in 1908, and engineer with T. H. Huston, General Contractor, Havana, in 1909. His association with the Cuban Telephone Company commenced in 1910, when he took charge of the company's long distance right of way. Subsequently he became cable engineer, plant engineer, Havana plant superintendent, general plant superintendent, assistant chief engineer, and chief engineer. He was named vice-president in 1927.

Henry D. Reed (A '94, M '13) president of the Bishop Wire and Cable Corporation, New York, N. Y., died April 27, 1943. Born in 1869 at Poughkeepsie, N. Y., he graduated from Stevens Institute of Technology in 1892. He immediately joined the Bishop Gutta Percha Company, which became the Bishop Wire and Cable Corporation in 1925, and was placed in charge of cable testing. From 1901 to 1908 he was general superintendent in charge of the manufacture of electric cables. He became vice-president of the company in 1909 and president in 1919. In addition he served as president of Bishop Industries, Inc., the Glenwood Holding Company, the No-Slip Rubber Company, and the Peter's Manufacturing Company.

Dana Ripley Bullen (A '02) retired assistant vice-president of the General Electric Company, Schenectady, N. Y., died May 10, 1943. Mr. Bullen was born at Wakefield, Mass., August 13, 1864, and received the bachelor of arts degree from Brown University in 1886. His entire career was spent with the General Electric Company and its predecessor the Thomson-Houston Electric Company, Lynn, Mass., where he entered the construction department in 1887. He was transferred to the commercial department in 1890 and remained there until 1915 when he became manager of the supply department. In 1923 he was appointed assistant vice-president. He retired in 1933. He had been a director of the Locke Insulator Corporation.

MEMBERSHIP

Recommended for Transfer

The board of examiners, at its meeting on May 20, 1943, recommended the following members for transfer to the grade of membership indicated. Any objection to these transfers should be filed at once with the national secretary.

To Grade of Fellow

DuBose, M., general manager, Saguenay Power Company, Montreal, Que., Canada.
Hazen, H. L., professor in charge, department of electrical engineering, Massachusetts Institute of Technology, Cambridge, Mass.
Housley, J. E., district power manager, Aluminum Company of America, Alcoa, Tenn.
Liversidge, H. P., president, Philadelphia Electric Company, Philadelphia, Pa.
Lyman, W. J., engineering assistant to manager, Duquesne Light Company, Pittsburgh, Pa.
Robertson, B. L., associate professor of electrical engineering, University of California, Berkeley, Calif.
Skeats, W. F., research engineer, General Electric Company, Philadelphia, Pa.
Stiner, H. W., electrical engineer, coal division, Koppers Company, Pittsburgh, Pa.

8 to grade of Fellow

To Grade of Member

Aicher, L. C., assistant electrical engineer, Allis-Chalmers Manufacturing Company, Milwaukee, Wis.
Arnold, E. J., division transmission and protection engineer, Michigan Bell Telephone Company, Saginaw, Mich.
Arthur, R. S., electrical sales engineer, General Electric Company, Schenectady, N. Y.
Baker, D. H., transmission and protection engineer, Michigan Bell Telephone Company, Detroit, Mich.
Bush, F. W., assistant to manager, electrical department, Allis-Chalmers Manufacturing Company, Milwaukee, Wis.
Dickinson, W. H., engineer, Standard Oil Development Company, Elizabeth, N. J.
Diehl, G. S., assistant to superintendent of operation, Pennsylvania Water and Power Company, Baltimore, Md.
Doan, G. H., assistant to superintendent of electric system, Detroit Edison Company, Detroit, Mich.
Donelson, L. E., toll transmission engineer, Michigan Bell Telephone Company, Detroit, Mich.
Elge, E. A., field engineering, power transmission department, General Electric Company, Pittsfield, Mass.
Estrada, H., superintendent of economy division, Philadelphia Electric Company, Philadelphia, Pa.
Gaty, L. R., assistant electrical engineer, Philadelphia Electric Company, Philadelphia, Pa.
Heine, G. M., electrical engineer, Cutler-Hammer, Inc., Milwaukee, Wis.
Johnson, J. O., chief product engineer, Aircraft-Marine Products Inc., Elizabeth, N. J.
Komives, L. I., electrical engineer, Detroit Edison Company, Detroit, Mich.
Leroy, E. R., acting vice-president in charge of engineering, Teleregister Corporation, New York.
MacGuffie, C. I., manager, industrial dept., General Electric Company, Schenectady, N. Y.
Marvin, H. B., assistant engineer, General Electric Company, Schenectady, N. Y.
McCoy, R. L., chief design engineer, Locke Insulator Corporation, Baltimore, Md.
Moore, R. C., electrical engineer, Allis-Chalmers Manufacturing Company, West Allis, Wis.
Rayner, R. L., transmission maintenance engineer, Michigan Bell Telephone Company, Detroit, Mich.
Sellers, J. F., engineer in charge of d-c engineering, electrical department, Allis-Chalmers Manufacturing Company, Milwaukee, Wis.
Shepherd, R. V., electrical engineer, General Electric Company, Schenectady, N. Y.
Sillers, T. G. A., assistant engineer, in charge, switchgear division, Allis-Chalmers Manufacturing Company, W. Allis, Wis.
Smith, W. C., engineer of foreign wire relations, Michigan Bell Telephone Company, Detroit, Mich.
Snell, D. S., electrical engineer, General Electric Company, Schenectady, N. Y.
Thiermann, A. H., control engineer, Virginia Electric and Power Company, Richmond, Va.
Thomas, D. E. F., senior research engineer, Detroit Edison Company, Detroit, Mich.
Warren, H. S., Phoenix-Arizona manager, Westinghouse Electric and Manufacturing Company, Phoenix, Ariz.
Weber, E., consulting engineer, Denver, Colo.
Welman, G., chief electrical inspector, Louisiana Rating and Fire Prevention Bureau, New Orleans, La.
Winn, L., chief of substation, Memphis Light, Gas and Water Division, Memphis, Tenn.
Woodward, J. S., electrical design engineer, General Electric Company, Schenectady, N. Y.

Wright, J. D., manager, industrial engineering department, General Electric Company, Schenectady, N. Y.
Zimmerer, C. W., assistant electrical engineer, Underwriters Laboratories, Inc., New York

35 to grade of Member

Applications for Election

Applications have been received at headquarters from the following candidates for election to membership in the Institute. Names of applicants in the United States and Canada are arranged by geographical District. Any member objecting to the election of any of these candidates should so inform the national secretary before June 30, 1943, or August 31, 1943, if the applicant resides outside of the United States or Canada.

To Grade of Member

Bullied, J. S., Capitol Elec. Co., San Diego, Cal.
Cannedy, R. E., Arthur McKee & Co., Cleveland, O.
Cockerline, H. B., Oregon State College, Corvallis, Oreg.
Cole, L. W., Cole Elec. Prod. Co., Inc., L. I. City, N. Y.
Cummings, B., Ill. Bell Tel. Co., Chicago, Ill.
Dace, F. E. (Member re-election), Bradley Polytechnic Inst., Peoria, Ill.
Denny, W. M., Gen. Elec. Co., Schenectady, N. Y.
Dewar, H. D., Gibbs and Hill, Inc., New York, N. Y.
Henry, P., Leach Relay Co., Los Angeles, Cal.
Hough, A. L., Shawinigan Water and Pr. Co., Montreal, Canada.
Howlett, P. W. (Member re-election), Sangamo Co., Montreal, Canada.
Jenks, D. W., Gen. Elec. Co., Schenectady, N. Y.
Johnson, R. E., Gen. Elec. Co., Los Angeles, Cal.
Joy, H. M., Master Elec. Co., Dayton, O.
Knight, J. B., Jr., U. S. Navy, Port Trumbull, New London, Conn.
Mack, E. A., Consult. Engr., Huntington, W. Va.
Mann, W. J., Jr. (Member re-election), Wolfe & Mann Mfg. Co., Baltimore, Md.
Martin, D., J. H. Bunnell & Co., Brooklyn, N. Y.
Mc Nutt, W. H., W. E. & M. Co., Cincinnati, O.
Mohler, F., Gen. Elec. Co., Schenectady, N. Y.
Orth, R. F., Johns-Manville Sales Corp., New York, N. Y.
Parker, W. A. H. (Member re-election), Craven House, Kingsway, London, England.
Peters, J. R. (Member re-election), City of Seattle Dept. of Ltg., Seattle, Wash.
Poliquin, A. L., Master Elec. Co., Dayton, O.
Rafn, J. S., Rafn-Morgan Elec. Co., Billings, Mont.
Smith, J. E., Kelso Wagner Co., Dayton, O.
Smith, R. P. (Member re-election), Westinghouse Elec. Supply Co., Atlanta, Ga.
Trone, D. (Member re-election), Internatl. Gen. Elec. Co., Schenectady, N. Y.
Varley, P., Canadian Industries, Ltd., Montreal, Canada.
Vellines, W. E., Jr., Chesapeake & Potomac Tel. Co., Charleston, W. Va.
Watson, H. E. (Member re-election), Gen. Elec. Co., Cincinnati, O.
Zimmerman, N. W. F., American Cyanamid Co., Bound Brook, N. J.

32 to grade of Member

To Grade of Associate

United States and Canada

1. North Eastern

Brewer, G. R., M.I.T., Cambridge, Mass.
Elliott, J. R., Gen. Elec. Ex-Ray Corp., Boston, Mass.
Fortenbach, J. H., Gen. Elec. Co., Schenectady, N. Y.
Grenfell, K. P., Gen. Elec. Co., Schenectady, N. Y.
Johnson, E. L., Conn. Pr. Co., New London, Conn.
Kulberg, M. E., Sylvania Elec. Products, Inc., Salem, Mass.
Losey, J. J., Gen. Elec. Co., Lynn, Mass.
Pope, K., N. E. Tel. & Tel. Co., Boston, Mass.
Scanlan, J. P., Mutual Boiler Ins. Co., Boston, Mass.
Shackelford, C. H., Gen. Elec. Co., Schenectady, N. Y.
Snyder, W. W. (Associate re-election), Niagara Alkali Co., Niagara Falls, N. Y.
Stauffer, L. H., Gen. Elec. Co., Schenectady, N. Y.
Vacca, R. H., Harvey Radio Labs., Inc., Cambridge, Mass.
White, M. L., W. E. & M. Co., Boston, Mass.

2. Middle Eastern

Baker, A. J., Ohio Power Co., Philo, O.
Barkley, W. J., Cons. Gas Elec. Lt. & Pr. Co., Baltimore, Md.
Boyer, C. S., Gen. Elec. Co., Philadelphia, Pa.
Breit, R. J., War Dept., Wright Field, Dayton, O.
Bryans, H. T., Philadelphia Elec. Co., Philadelphia, Pa.
Burke, D. C., Gen. Elec. Co., Philadelphia, Pa.
Campbell, C. H., Susquehanna Elec. Co., Conowingo, Md.
Carlson, J. T., Master Elec. Co., Dayton, O.
Cherones, W. J., Air Serv. Com., Patterson Fd., Fairfield, O.
Chromick, J., Frigidaire Pt. #2, Moraine, O.
Clark, L. D., Master Elec. Co., Dayton, O.
Dunn, J. W., Greenville Elec. Lt. & Pr. Co., Greenville, O.
Dunning, R. P., Graybar Elec. Co., Inc., Dayton, O.
Feldstein, S. V., Gen. Elec. Co., Philadelphia, Pa.
Hamm, G. B., Wright Field, Dayton, O.
Hayn, G. J., Gates & Shepard, Philadelphia, Pa.
Heindl, J. J., Jr., Cons. Gas & Elec. Co., Baltimore, Md.
Henry, L. H., Genl. Motors, Dayton, O.
Keel, J. J., Western Elec. Co., Baltimore, Md.
Kelley, S. B., U.S.N., Neville Is., Pittsburgh, Pa.
Killen, R. B. (Associate re-election), Dayton Pr. & Lt. Co., Dayton, O.
Lang, J. O. (Associate re-election), Dayton Pr. & Lt. Co., Dayton, O.
Major, C. B., Brown & Heim, Inc., Baltimore, Md.
Medley, E. H., Lear Avia. Inc., Piqua, O.
Milroy, P. R., Gen. Elec. Co., Erie, Pa.
Munhall, D. J., Natl. Carbon Co., Cleveland, O.
Noren, T. E., American Tel. & Tel. Co., Dayton, O.
Oxley, K. G., Dayton Pr. & Lt. Co., Dayton, O.
MacMillan, N., Natl. Cash Register Co., Dayton, O.
Marshment, B. C., U. S. Navy, Cincinnati, O.
Menard, G. A., Gen. Elec. Co., Philadelphia, Pa.
Nelson, R. H., Westinghouse Radio Div., Baltimore, Md.
Noy, J. M. (Associate re-election), Climax Molybdenum Co., Langeloth, Pa.
Parsons, H. S., Whiteley Elec. Co., Baltimore, Md.
Peirce, C., War Dept. Wright Field, Dayton, O.
Pottle, C. J., Frigidaire Pt. #2, Dayton, O.
Puterbaugh, W. H., Jr., Natl. Cash Register Co., Centerville, O.
Rappaport, H., Penn. R.R., Harrisburg, Pa.
Schindler, H. J., War Dept., Wright Field, Dayton, O.
Sprinkel, E. R., Frigidaire Pt. #2, Dayton, O.
Squibb, R. W., American Rolling Mill Co., Zanesville, O.
Surber, W. H., Jr., Princeton Univ., Princeton, N. J.
Teszak, J. C., Navy Dept. Bureau of Ships, Washington, D. C.
Unger, J. J., Elect. Prod. Co., Cleveland, O.
Vaughn, H. R., Line Material Co., Zanesville, O.
Warner, R. M., Dravo Corp., Wilmington, Del.
Weddell, J. A. (Associate re-election), Goodyear Tire & Rubber Co., Akron, O.
Wojcik, H. L., Air Serv. Com., Patterson Fd., Dayton, O.
Yetter, J. W., Gen. Elec. Co., Philadelphia, Pa.

3. New York City

Adams, L. F., U. S. Navy Yard, Brooklyn, N. Y.
Anderson, R. I., Sperry Gyroscope Co., Inc., Brooklyn, N. Y.
Bachman, C. V., Orange & Rockland Elec. Co., Monroe, N. Y.
Bank, H. M., 754 Lexington Ave., Brooklyn, N. Y.
Blanchard, T. G., Bell Tel. Co., Inc., New York, N. Y.
Carman, R. C., Ward Leonard Elec. Co., Mt. Vernon, N. Y.
Doyle, P. W. (Associate re-election), Cox & Stevens, Inc., New York, N. Y.
Emerson, J. A., Burns & Roe, New York, N. Y.
Green, J. B. P., Abbott Merkt & Co., New York, N. Y.
Haylock, F. B., Gage Publishing Co., New York, N. Y.
Hliddal, T., I. T. & T. Corp., New York, N. Y.
Hoffmann, J. C., Forstmann Woolen Co., Passaic, N. J.
Kandel, E., Kandel Bros., Middletown, N. Y.
Kerner, G. H., Western Elec. Co., Kearny, N. J.
Klemantowitz, W., Western Elec. Co., New York, N. Y.
Koch, R. H., J. G. White Engr. Corp., New York, N. Y.
Lake, R. E., Pratt Inst., Brooklyn, N. Y.
LoMedico, T. J., G. G. Sharp, New York, N. Y.
O'Hara, T. P., U. S. Navy Yard, Brooklyn, N. Y.
Patrone, E. F., U. S. Navy Yard, N. Y.
Powell, P. T., U. S. Rubber Co., Passaic, N. J.
Proctor, P., Proctor Elec. Co., Asbury Park, N. J.
Rabinovitch, V. S., Mining Equipment Corp., New York, N. Y.
Reber, J. F. (Associate re-election), Chemical Const. Corp., New York, N. Y.
Ryan, E. H., U. S. Govt., Navy Dept., Navy Yard, N. Y.
Ryan, J. R. C., Techno Scientific Co., Yonkers, N. Y.
Stuart, W. T., McGraw-Hill Pub. Co., New York, N. Y.

4. Southern

Emmons, P. A., Jr., Carolina Aluminum Co., Badin, N. C.
McCartney, C. B., Fla. Pr. Corp., St. Petersburg, Fla.
Raffalovich, E. V., Tenn. Valley Auth., Wilson Dam, Ala.
Roberts, W. F., Tenn. Valley Auth., Chattanooga, Tenn.

5. Great Lakes

Beach, G. H., Commonwealth Edison Co., Chicago, Ill.
Cassidy, C. F. (Associate re-election), Gen. Elec. Co., Chicago, Ill.
Hurst, E. H., Hartford Steam Boiler Inspection & Ins. Co., Detroit, Mich.
McNeill, E. H., The Okonite Co., Chicago, Ill.
Petry, H., Engr., Rushville, Ind.
Rachford, J. K., E. I. DuPont & Co., Charlestown, Ind.
Schmitz, N. L., Cutler-Hammer, Inc., Milwaukee, Wis.
Vanderbeek, R. O., H. A. Douglas Mfg. Co., Bronson, Mich.

7. South West

Anderson, R. L., Lower Col. River Auth., Austin, Tex.
Brown, F. W., Kansas Gas and Elec. Co., Wichita, Kans.
Coate, L. W., Butler Mfg. Co., Kansas City, Mo.
Harris, H. W., University of Mo., Columbia, Mo.
Horn, R. T., St. Joseph Pr. & Ltg. Co., St. Joseph, Mo.
Haynes, C. M., Empire Dist. Elec. Co., Baxter Springs, Kans.
Oliver, W. H., A. T. & T. Co., St. Louis, Mo.
Shriver, W. B., W. E. & M. Co., Houston, Tex.

8. Pacific

Gagnier, C. E. (Associate re-election), No. Amer. Avia., Inc., Inglewood, Cal.
Gindroz, F. H., Elec. Engg. & Mfg. Corp., Los Angeles, Cal.
Glaze, L. B., No. Amer. Avia., Inc., Inglewood, Cal.
Gragg, W. C., Goodyear Aircraft Corp., Litchfield Park, Ariz.
Grane, M. M., U. S. Navy, San Francisco, Cal.
Hanna, H. F., Pac. Naval Air Bases, Alameda, Cal.
Holley, C. C., Aluminum Co. of America, Los Angeles, Cal.
Johnston, B. W., Douglas Aircraft Co., Santa Monica, Cal.
King, S. C., Douglas Aircraft Co., Santa Monica, Cal.
Mangum, O. K., U. S. Bur. of Reclamation, Phoenix, Ariz.
Maurice, G., Elec. Engg. & Mfg. Corp., Los Angeles, Cal.
McEntee, L. C. (Associate re-election), Consolidated Aircraft, San Diego, Cal.
McMullin, E. K., No. Amer. Aviation, Inc., Inglewood, Cal.
Morairty, A. F., Central Arizona Lt. & Pr. Co., Phoenix, Ariz.
Morris, F. W., Southern Cal. Edison Co., Los Angeles, Cal.
Nash, A. W., Douglas Aircraft Co., El Segundo, Cal.
Nothmann, R. S., Sawyer Elect. Mfg. Co., Los Angeles, Cal.
Payse, V., Jr., No. Amer. Avia., Inc., Inglewood, Cal.
Phillips, E. C., Coast Elec. Co., San Diego, Cal.
Piper, R. I., Southern Cal. Tel. Co., Los Angeles, Cal.
Rogers, S., Dr. Lee de Forest, Hollywood, Cal.
Russell, W. A., Jr., Douglas Aircraft Co., Santa Monica, Cal.
Seibert, R. C., Cutler-Hammer Co., Los Angeles, Cal.
Shinn, G. C., Roosevelt Base, Terminal Island, Cal.
Strader, M., Jr., Aluminum Co. of America, Torrance, Cal.
Strauss, F. E., Gen. Elec. Co., San Francisco, Cal.
Taylor, D. H., Douglas Aircraft Co., Los Angeles, Cal.
Yates, C. T., Cent. Arizona Lt. & Pr. Co., Phoenix, Ariz.

9. North West

Applegate, W. A., U. S. Army Engrs., Portland, Ore.
Ball, R. M., Montana Pr. Co., Butte, Mont.
Benson, L. B., Anaconda Wire & Cable Co., Seattle, Wash.
Carte, W. A., Siems Drake Puget Sound Co., Seattle Wash.
Converse, W. H., Jr., Siems Drake Puget Sound Co. Seattle, Wash.
Fahlstrom, M. E., Diablo Pr. Plant, Rockport, Wash.
Hudson, H. M. (Associate re-election), Harold M. Hudson Co., Seattle, Wash.
Klaunig, W. E. J., Associated Shipyards, Seattle, Wash.
Mosley, W. J., Puget Sound Pr. & Lt. Co., Seattle, Wash.
Rydell, R. W., Spokane Army Depot, Spokane, Wash.
Scofield, E. E., Washington Water Pr. Co., Spokane, Wash.
Temple, R. B. (Associate re-election), Gen. Elec. Co., Portland, Ore.
Walker, R. A., Puget Sound Pr. & Lt. Co., Seattle, Wash.

10. Canada

Hastings, A. S, Canadian Comstock Co., Montreal, Canada.
Kirlin, G (Associate re-election) Canada Wire and Cable Co., Montreal, Canada.
Morden, W. E. Phillips Elec. Works, Ltd., Brockville, Ont.
Walters, W. H., Northern Elec. Co., Ltd., Montreal, Canada.

Elsewhere

Rabay, A. N., Gen. Elec. Co., S/A, Rio de Janeiro, Brazil.
Simmons, H. G., Distributing Dept. Elect. Board, Trinidad, B. W. I.
Valdes, J. L., Westinghouse Elec. Intl. Co., Santiago, Chile.
O'Bourke, W. G., Cable & Wireless West Indies Ltd., Havana, Cuba.
Penzig, O. D., Gen. Elec. Co., Ltd., Witton, Birmingham, England.
Meeks, C. A. L., Westmorland & Dist. Elec. Supply Co., Ltd., Carnforth, Lancashire, England.
Stewart, R. H., English Elect. Co., Ltd., Stafford, England.
Sidhu, H. S., Indian Inst. of Sciences, Bangalore, India.

Total to grade of Associate
United States and Canada, 155
Elsewhere, 8

OF CURRENT INTEREST

Foreign Patents Offered to U. S. Industry

Putting the patents formerly vested in enemy nationals or nationals of enemy-occupied countries to work for the United Nations war effort, is the major task yet to be accomplished in combating the economic penetration of American industry by enemy interests, according to a recent report by the Office of the Alien Property Custodian and the Treasury Department who are charged with the handling and disposal of enemy property.

A number of small firms have been able through patent licenses obtained from the Custodian to convert their plants to war production. Some of the products and processes covered in these conversions are collapsible boats for the Navy, fire-fighting materials, machining crankshafts, materials used in dressing ores, manufacture of alumina for airplanes, and heat-treating facilities for metal working.

The patents in use, however, represent but a small part of the 41,077 patents and pending patent applications covering inventions in every field of applied science, which have been vested and are under the control of the Custodian. These patents symbolize the expenditure of millions of dollars and millions of man-hours of research by the finest scientific minds of countries under Axis domination. They cover some of the latest achievements in the production of dyestuffs, plastics, pharmaceuticals, electrical goods, and textiles. Many patents on processes that have enabled the enemy to carry on mechanized warfare while cut off from petroleum, rubber, and other raw materials are included. The figures released by the Custodian reveal the proportion of patents in different fields: 1,282 are in the field of ordnance and aeronautics, 25,423 are related to manufacture, transportation, power, and machinery, 6,647 are chemical in nature, 836 metallurgical, and 6,889 electrical.

As indicative of the importance such patents could assume in the national economy, the office of the custodian listed some of the American companies whose progress has been aided by German patents seized during World War I. In the last war patents vested from the enemy were licensed to producers on an exclusive basis. However, at present the government is following a policy designed to make the scientific advances latent in the patents more generally felt in American industry as a whole. As many industrial producers as possess the means of employing a patented device may obtain a patent license on a nonexclusive basis, and royalty-free terms.

Patents which have been vested from the nationals of enemy-occupied countries will be licensed on the same non-exclusive, royalty-free basis except that the license expires six months after the war ends. At present the government is negotiating the question of the royalties on such patents with the governments in exile of the countries involved.

The Office of the Alien Property Custodian has issued the following simplified outline of the procedure to be followed by those interested in securing licenses to any of the vested patents:

1. Write to the Office of the Alien Property Custodian, Chicago, Ill., for an Index of Patents vested in the Alien Property Custodian.

2. Write to the same address for classified lists of vested patents and patent applications in which you are interested. All classes are sold for ten cents each, except nine classes which are 25 cents each.

3. Printed copies of vested patents and drawings and specifications of vested patent applications may be secured from the Commissioner of Patents, Washington, D. C., for ten cents each.

4. Send to the Office of Alien Property Custodian, Washington, D. C., your letter of application for a license to use a vested patent. Application fee of $50, plus $5 for each related patent to be covered in one license should accompany application.

WPB Production Clinic Program Described at District Meeting

Reviewing the success of the program of co-operation among industrial operators through problem clinics inaugurated by the War Production Board, T. M. Linville (M '34), Schenectady Section Secretary, in a paper presented at the North Eastern District Technical Meeting, Pittsfield, Mass., on April 8, covered the arrangements made by WPB for financial and other assistance, local arrangements and program topics, and the organization of the Schenectady clinic. He emphasized the fact that, while normal industrial progress requires competition, war production benefits from co-operation.

At the outset of the program the WPB decided to work through the national engineering societies to effect an exchange of experience that would make available to all operators the best solution to specific production problems. The co-operating national societies were: the American Society of Mechanical Engineers, the Society of Automotive Engineers, the American Institute of Chemical Engineers, the American Society of Civil Engineers, the American Institute of Electrical Engineers, and the American Institute of Mining and Metallurgical Engineers. The engineering societies were selected as best fitted for the work although they would be acting for nonmembers more often than for their own members.

Under the terms of the contract outlined by Mr. Linville, the normal budget of a clinic is $200. This cost is borne by the WPB. Any dinners or luncheons connected with the clinics are self-sustaining. Program subjects must adhere strictly to shop talk about production methods and problems. Topics chiefly administrative, war contracts, priority difficulties, labor relations are not to be discussed. WPB and the Office of War Information assist with publicity. In addition Mr. Linville mentioned a pamphlet, "Suggestions for Organization and Conduct of War Production Conferences," published by WPB which gives a synopsis of the duties of the essential committees. Any one national society or group of societies may propose a clinic. A WPB representative will then visit the city chosen for the clinic to act as liaison agent.

From his experience in organizing the recent Schenectady, N. Y., clinic (*see page 265*), Mr. Linville recommended that the national societies organize panels separately. However he stated that the broadest possible sponsorship pays the highest dividends in attendance and interest if responsibility can be properly allocated. Besides a discussion leader, several panel members are needed for each session. The latter should be prepared to present problems for about ten minutes each and should possess wide experience in the subject discussed.

Attendance, which Mr. Linville believes is the surest measure of success, has varied from 500 to 2,500 at the clinics already held in 19 of the 37 cities picked by the WPB, since the first one at Newark, N. J. (*EE, July '43, pp. 382–3*). As illustrative of the direction of interest at the clinics, Mr. Linville cited the sessions best attended at representative clinics. These were: electrical problems and accident prevention at Philadelphia, Pa., maintenance and machine-shop problems at Indianapolis, Ind., selection and training of employees and product inspection at Hartford, Conn., women in industry and electric-power machinery at Providence, R. I., machine-shop problems and getting the most out of cutting tools by welding and brazing, protective metal coatings, and problems in drilling and tapping at Chicago, Ill.

In conclusion Mr. Linville expressed his belief in the practical value of the team spirit engendered by industrial operators meeting with others faced with problems similar to their own.

Navy Offers Commissions to Electronic Specialists

Some of the billets for Naval officers trained in electrical engineering (*EE, Feb. '43, p. 81*) created by the rapid expansion in the number of vessels serviced by ultra-high frequency apparatus are still to be filled according to a recent release from the Office of Naval Procurement.

Men who hold degrees in electrical engineering or men with degrees in mathematics, physics, or other branches of engineering who have acquired a sound working knowledge of a-c circuits and electronics may apply for commissions. As previously reported those accepted will be given the Navy's three-months' course in ultrahigh frequencies.

Applications for the commissions are being received at local offices of Naval Officer Procurement in the following cities throughout the United States:

Atlanta, Ga.
Charleston, S. C.
Columbia, S. C.
Jacksonville, Fla.
Raleigh, N. C.
Boston, Mass.
Chicago, Ill.
Cincinnati, Ohio
Cleveland, Ohio
Des Moines, Iowa
Detroit, Mich.
Kansas City, Mo.
Milwaukee, Wis.
Minneapolis, Minn.
St. Louis, Mo.
Los Angeles, Calif.
Phoenix, Ariz.
San Diego, Calif.
Miami, Fla.
New Orleans, La.
Birmingham, Ala.
Dallas, Tex.
Houston, Tex.
Memphis, Tenn.
Nashville, Tenn.
Oklahoma City, Okla.
New York, N. Y.
Rochester, N. Y.
Philadelphia, Pa.
Pittsburgh, Pa.
Richmond, Va.
Charleston, W. Va.
Norfolk, Va.
San Francisco, Calif.
Seattle, Wash.
Portland, Oreg.
Washington, D. C.
Baltimore, Md.

Planning Board Formed by Radio Industry

That a Radio Technical Planning Board which will chart the postwar course of frequency modulation, television, and other recent advances in radio has been formed by the Radio Manufacturers Association and the Institute of Radio Engineers with the co-operation of the Federal Communication Commission, was announced by FCC Chairman J. L. Fly at the recent annual meeting of the National Association of Broadcasters in Chicago, Ill.

The Board, which has a scope similar to that of the prewar National Television System Committee, will seek to avoid delay in passing on the recent technical developments in radio to the public after the war. By proper allocation of operating frequency and systems standardization the chaos which existed in the early nineteen twenties will not be repeated.

The Board will function through a chairman appointed by the presidents of the RMA and the IRE with the approval of the FCC, and members appointed by the Chairmen, representing recognized radio groups, and the chairmen of the panels. These panels will do the research necessary to formulate recommendations for government agencies and industrial and professional organizations. Panel membership will be appointed by the panel chairmen who will be appointed by the Board chairman. At present, panels on allocation, spectrum utilization, high frequency generation, television and facsimile, direction finding and location, industrial, medical, and scientific equipment, standard broadcasting, relay systems, and radio communication are contemplated. Government representatives may attend the sessions of the board or the panels but they are not entitled to vote on any matter under consideration. Additional panels will be formed as they are needed.

The project is being financed by the RMA and an appropriation of $10,000 has already been made to defray RTPB expenses.

Capacitors Raise Transmission Capacity and Save Metals

Experimenting with a midget power line, R. D. Evans (F '40) consulting transmission engineer, Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa., recently demonstrated a method of increasing the capacity of a transmission system by means of thousands of small capacitor units distributed along the line.

A comparison of costs based on Mr. Evans' estimates shows the savings that might result from the use of this method to increase the capacity of a typical line. The cost of increasing the capacity of a 250-mile-long power line 65 per cent by an additional transmission line plus auxiliary equipment would be about $4,500,000. By installing 7,200 capacitors instead, the same results can be attained for $1,200,000. The cost of a generator needed in both instances is not included in these appraisals.

Substituting the capacitor units for extra transmission wires also conserves the more essential metals, Mr. Evans pointed out. While 7,200 capacitors can be built from nine tons of copper and brass, 175 tons of steel, and a few tons of aluminum, extra power lines would require 2,900 tons of copper and 7,600 tons of steel.

The new device which secures the capacitors in steel structures beneath the power line was made practicable by a recently developed protective system in which switches automatically cut off the bank of capacitors from a circuit when a short circuit occurs. Service is again automatically restored when the trouble is removed. Mr. Evans collaborated with A. C. Monteith (M '40) and R. E. Marbury (M '36) in developing this protective system.

Mica Supply Still Barely Adequate

Although immediate erection of a plant which will produce "Polectron," synthetic substitute for mica, has been announced by the General Aniline and Film Corporation, New York, N. Y., the mica-graphite division of the War Production Board has issued a warning that no end to the heavy drain on the critically low stock of high-grade mica may be expected.

The Board pointed out that none of the new materials now in process of development can be used as an all-around substitute for mica. In addition there has been little actual adoption either by the armed services or by prime contractors of components containing any of these new materials as a replacement for mica. Even when estimates on the percentage of substitution that now seems probable in the near future are taken into account the remaining requirements of war industries will absorb all anticipated mica production.

The Aniline corporation's product "Polectron" combines high temperature resistance with low dielectric loss which make it useful in several types of radio equipment now requiring mica. Polectron can be employed in the production of electronic apparatus without fundamental change in machines or methods of manufacture. One ton of "Polectron" fabricated into mica replacement material will save from 10 to 15 tons of imported block mica which often must be flown from India or Brazil.

Government Has $7,000,000,000 Invested in War Plants

The $7,000,000,000 which the United States Government has invested in 1,479 war plants portends an extensive government interest in postwar industry, Secretary of Commerce Jesse Jones indicated in an Army Day address delivered at New York.

According to Mr. Jones the Defense Plant Corporation, a subsidiary of the Reconstruction Finance Corporation, has financed 54 plants for radio and radio equipment with an investment of $60,000,000. Other plants so subsidized are 51 airplane plants, 344 plants for making airplane parts, 64 air school plants, 70 ordnance plants, 161 plants for manufacturing tools, and 43 plants for aluminum manufacture. Investment in plants for airplanes and parts, instruments and accessories, totaled $2,640,000,000 while the total investment for guns, ammunition, tanks, and armor was $440,000,000.

In Mr. Jones's opinion the government should neither own nor operate the facilities it has constructed but should expect to work with private industry in operating these plants.

Automatic Control Sights Antiaircraft Guns

A mechanical brain that will sight an antiaircraft gun automatically, accurately, and instantly is being developed by the engineers of the Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa. The distance to the target, its rate of speed, its direction of travel up or down, the amount of windage, the force of gravity, the gunner's own speed and direction if any, all hitherto separate computations will be part of one automatic operation.

Instruments will provide the gun operator with the actual angular position of a nearby radio antenna recording the foregoing facts. This antenna either remains fixed in position, rotates continuously at speeds varying from one revolution per minute to one every six hours, or swings back and forth through a selected arc at a chosen speed.

The controls and drive will work accurately even when wind velocities rise to 125 miles per hour, and temperatures fall far below zero.

The heavy gun turret which turns slowly if hand-propelled swings by electric power under the new control scheme. Some older type guns can be corrected to the sighting mechanism by a hydraulic torque amplifier or oil-driven gear.

Chinese Engineers to Study American Techniques

Under a program developed jointly by the National Resources Commission of China and the Board of Economic Warfare, 32 young Chinese engineers have arrived in the United States to study American engineering techniques which they intend to employ in rebuilding China, the Board announced recently.

The engineers expect to remain two years working in their major fields of interest. Eight of the group who expressed interest in electric power generation and transmission have taken positions with the Tennesseee Valley Authority. Others are studying production of electrical equipment, mechanical and chemical engineering, metallurgy, mining, petroleum, and industrial management with privately owned industrial firms.

The young men selected for the training program received advanced training in Chinese technical schools and possess several years of actual experience in their chosen fields. Since the industries to which they have been assigned are engaged in war work, the men are first accredited by the United States Army. They are paid by the National Resources Commission of China. The program was first sponsored by the Division of Cultural Relations of the United States Department of State.

Conservation Division of WPB Reorganized

In order to establish a parallel relationship between the conservation of materials and the conservation of products manufactured from these materials, the Conservation Division of the War Production Board has been reorganized into two main branches, materials and products, Howard Coonley, director of the Division has announced.

Material substitution, product specification, and product simplification, which are the three main objects of the division, will cut across the work of the two major branches. This will result in greater coordination of the assistance given to the Division by its consultants. The latter will be called upon for advice on both materials and related products when industrial production planning is undertaken.

Each consultant will continue to be responsible for conservation planning within a specific field of industry. Two-thirds of these consultants are technical specialists. Before the war, 36 of them were professional engineers, 12 were chemists, while others were metallurgists, architects or industrial engineers. The average span of specialized experience among them is 20 years.

Radio Parts Now Soldered By Electronic Heat

Electronic high-frequency induction heat will control some of the difficulties inherent in employing gas heat in soldering the crystal units which maintain frequency in war-radio equipment, J. E. Jordan (A '42), engineer, General Electric Company, Schenectady, N. Y., recently explained.

When gas heat is used to solder the metal shield for the crystal units, there is an ever-present danger of distortion resulting from the slow heat's being conducted to the crystal, he pointed out. Injurious effects may also result from the products of high-temperature gas combustion.

Under the new process, Mr. Jordan disclosed, the crystal unit is placed in a fixture adjacent to a two-turn inductor coil and a perforated air-blast ring nozzle. Heat and a cooling air blast alternately reach the metal of the unit for a few seconds at a time. The entire sequence is automatically timed to assure uniform seals. During the heating cycle the operator twists the shell slightly to distribute the solder.

Future Meetings of Other Societies

American Association for the Advancement of Science. June 14–19, 1943, Corvallis, Oreg.

American Mathematical Society. June 16–17, 1943, Corvallis, Oreg.

American Chemical Society. September 6–10, 1943, Minneapolis, Minn.

American Institute of Mining and Metallurgical Engineers. Joint fuels conference, October 28–29, 1943, Pittsburgh, Pa.

American Society for Metals. Annual convention, October 18–22, 1943, Chicago, Ill.

American Society of Heating and Ventilating Engineers. Semiannual meeting, June 6–7, 1943, Pittsburgh, Pa.

American Society of Mechanical Engineers. Semiannual meeting, June 14–16, 1943, Los Angeles, Calif.

American Society for Testing Materials. Annual meeting, June 28–July 2, 1943, Pittsburgh, Pa.

American Society of Refrigerating Engineers. 30th spring meeting, June 1–3, 1943, Cleveland, Ohio.

Canadian Electrical Association. Annual meeting, June 17–18, 1943, Quebec, Que.

Edison Electric Institute. Annual meeting, June 2–4, 1943, New York, N. Y.

Illuminating Engineering Society. Great Lakes regional conference, June 5, 1943, Detroit, Mich.; northeastern regional conference, June 11, 1943, New York, N. Y.

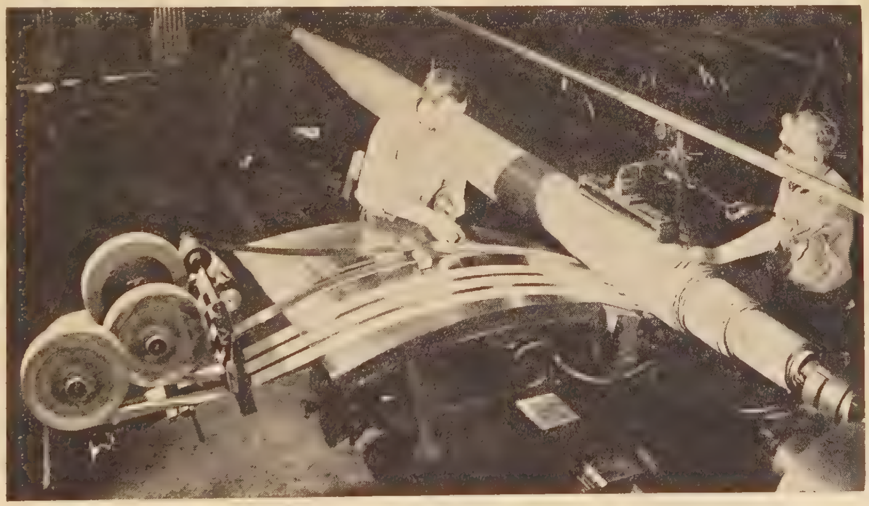
Society for the Promotion of Engineering Education. Annual meeting, June 18–20, 1943, Chicago, Ill.

Society of Automotive Engineers. Diesel engine, fuels, and lubricants meeting, June 2–3, 1943, Cleveland, Ohio.

ASA Completes Standard for Radio Parts

In order that home radio sets may be serviced with the least possible employment of scarce materials essential to the war effort, the American Standards Association has issued a war standard for radio parts entitled, "Power and Audio

Oil Used in Impregnating Condenser Bushing



These men are spiral-winding a large impregnated condenser bushing with paper and copper foil. The paper used in this process was formerly impregnated with shellac imported from India. By using oil instead the Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa., saves 144,000 pounds of shellac annually. Tests have shown that electrical qualities of the new bushing, particularly the power factor, are superior to older types

Transformers and Reactors, Home Receiver Replacement Type.” The standard was compiled at the request of the Office of Price Administration.

Covering the performance and quality of a simplified list of 14 such parts, the standard is expected to meet the needs of about 90 per cent of the radio sets in operation. The standard was drawn up with the intention of using a minimum of such materials as copper and transformer steel, that might be allocated to radio replacement parts. The simplified list of units will also mean fewer production lines and smaller dealer inventories.

Used Motor Unit Facilitates Production and Saves Materials

The Used Motor Unit of the War Production Board recently announced a successful first full month of operation. Of 1,515 requests for used motors and generators the unit was able to fill all but 87.

By placing idle equipment in active service in industry at large and in over 29 war agencies, the unit which was established in February has saved 400 tons of critical materials and approximately 40,000 man-hours which would have been necessary to manufacture new equipment. At present 30,000 to 35,000 pieces of idle equipment are listed, and requirements for general-purpose standard types of second-hand motors can be filled quickly. However owners of idle equipment are urged to register it immediately.

Tantalum Mined in New Mexico. With the recent discovery of tantalum ore in New Mexico, manufacturers of special carbides and electrical equipment may anticipate an increased supply of that rare material, the United States Bureau of Mines has announced. By experimenting with 30 tons of ore from which over three tons of high-grade concentrate were extracted, the Bureau has worked out a treatment process in detail in its pilot plant at Rolla, Mo. The concentrate which is stockpiled at Rolla has been priced at $3.50 a pound or $7,000 a ton by the Metals Reserve Company, a federal agency, which has undertaken disposal of the mineral.

LETTERS TO THE EDITOR

INSTITUTE members and subscribers are invited to contribute to these columns expressions of opinion dealing with published articles, technical papers, or other subjects of general professional interest. While endeavoring to publish as many letters as possible, Electrical Engineering reserves the right to publish them in whole or in part or to reject them entirely. Statements in letters are expressly understood to be made by the writers. Publication here in no wise constitutes endorsement or recognition by the AIEE. All letters submitted for publication should be typewritten, double-spaced, not carbon copies. Any illustrations should be submitted in duplicate, one copy an inked drawing without lettering, the other lettered. Captions should be supplied for all illustrations.

The Anniversary of a Symbol

To the Editor:

It has been brought out everywhere that the year of turmoil, 1942, marked the 60th anniversary of Edison’s Pearl Street power house and that of the first transmission line. But no mention was made that the same year marked the 50th anniversary of the introduction of the square root of minus one into electrical engineering by two young American engineers, as recorded casually in *Electrical Engineering*, December 1942.

Perhaps the introduction of the $x+jy$ will eventually be considered more important than the other two, or at least as important. When we analyze the importance of inventions in the development of the electrical industry, which means practically all industry, we must give the analytical invention at least equal credit with that of physical invention. Would electrical engineering be what it is today without Maxwell or Heaviside? Would we have radio if Maxwell had not laid the mathematical foundation for it? Would the art of transmission and interconnection engineering be where it is today if Bedell and Crehore had not introduced the square root of minus one back in 1892? In this period of racial-superiority talk by others, it is interesting to note that a-c engineering is practically all American. It is also interesting to note that the two young American engineers who introduced the $x+jy$ did so as a matter of course, without proclaiming from the house tops that America is the source of all science. They did it with the modesty common to all young American engineers, inventors, and scientists. This very resourcefulness, unlimited, is our greatest hope in the war. Even now, in a new paper published in the December 1942 issue of *Electrical Engineering*, Professor Bedell, very modestly, gives the fact a mere mention that he and his colleague practically created modern a-c engineering. He quotes from a mimeographed paper presented May 8, 1934, before the Los Angeles Section of the AIEE by P. H. Nunn:

“In 1890 alternating current was just plain freak; it did not follow Ohm’s law and ‘clogged’ itself in its circuits. Bedell and Crehore had doped out its laws and demonstrated their concepts in 100 pages of solid calculus.”

Again he shows his great modesty by reporting that the $\sqrt{-1}$ has been used in astronomy back in 1797 and proceeds with his modesty by saying:

“It is to A. E. Kennelly, however, that credit should be given for bringing out its full significance in the application of complex quantities to a-c problems and to C. P. Steinmetz for so ably extending its usefulness.”

The fact remains nevertheless that Bedell and Crehore invented it for the electrical engineer. It would be well for the young electrical engineer to stop for a moment and think where he would be without the $x+jy$. Our hats are off to Bedell and Crehore, the great, modest, competent, American pioneers, and many happy returns to them!

M. M. SAMUELS (F ’24)

(Chief, technical standards division, Rural Electrification Administration, St. Louis, Mo.)

Inventions, Patents, and the Engineer

To the Editor:

In his most interesting contribution to the discussion of “Inventions, Patents, and the Engineer,” Ford W. Harris gives expression to a sentiment that I feel sure he will want to revise after due consideration. He says:

“We can admit that the desire for profit is low and ignoble.”

Although he modifies this statement somewhat by the unquoted part of the sentence, the impression is left that “the results produced up to date, etc.,” were engendered by ignoble motives. That, of course is not true.

The notion that “profit” is ignoble is a relic of the false notion that in any business transaction the seller gains and the purchaser loses. If that were true, every purchaser would progressively grow poorer and poorer until all business would cease. The truth is just the reverse. Profit is the result of enterprise which benefits others as well as the enterpriser.

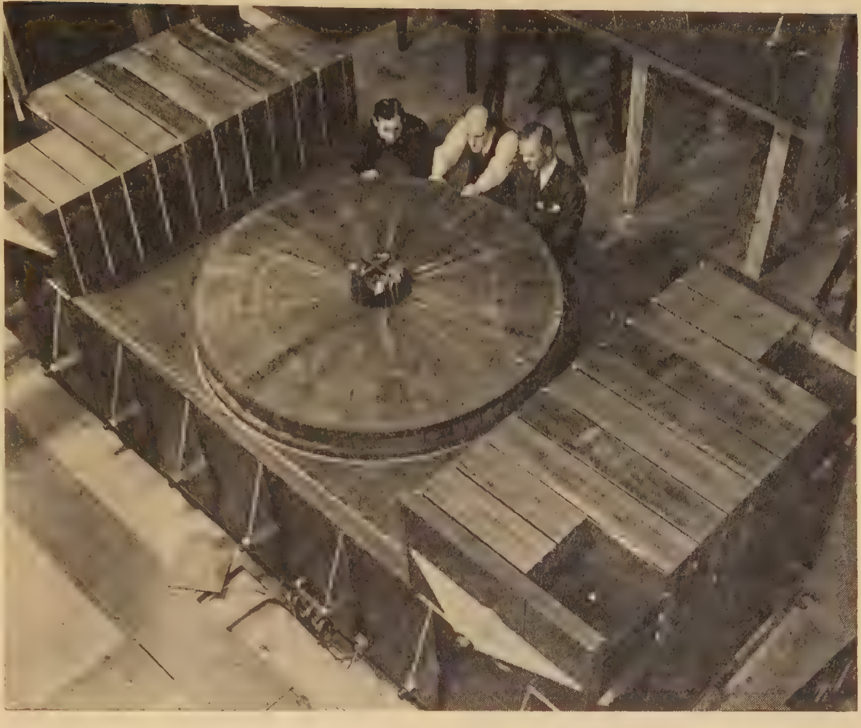
For example: Electrical rates have been decreasing almost continuously since the beginnings of the electrical industry. At first electric energy was used almost exclusively for lighting, and consequently the rates were high, necessarily so. Here in Madison 33 years ago a kilowatt-hour of electric energy cost 16 cents. As the uses of electric energy increased, and as the means of its production was improved, the rates dropped until today a kilowatt-hour costs only a trifle more than two cents. Profits made this reduction in rates, extension of service, and improvement in service possible. Again the first tungsten lamp cost me two dollars, and today I can buy a better lamp, even in a drug store, for from 10 to 15 cents. This reduction in price and improvement in the article was engendered by profits and not ignoble motives. Without profits there would be no electrical industry, and everyone would be the poorer.

TVA is a nonprofit enterprise. That is, its stockholders, the taxpayers, have received no dividends. Does that fact make TVA’s motives more noble than those of the Consolidated Edison Company which pays dividends and millions of dollars in taxes?

C. M. JANSKY (F '32)

(Professor emeritus of electrical engineering, University of Wisconsin, Madison, Wis.)

100,000,000-Volt X-Ray Machine



This large electron accelerator which will weigh 130 tons when completed will generate X rays at voltages up to 100,000,000, making possible the examination of thicker metal sections than previously could be studied by X rays. The picture shows the lower part of the huge a-c magnet, made of slabs of steel spaced apart for cooling, and consisting of thin sheets cemented together. Examining one of the circular magnet pole faces, between which the electrons are whirled around in a six-foot doughnut-shaped glass vacuum tube are (left to right) Rudolph Wroblewski, who is assisting in the erection of the device, and W. F. Westendorp (A '35) and Ernest E. Charlton of the laboratory's X-ray section

NEW BOOKS . . .

The following new books are among those recently received from the publishers. Books designated ESL are available at the Engineering Societies Library; these and thousands of other technical books may be borrowed from the library by mail by AIEE members. The Institute assumes no responsibility for statements made in the following summaries, information for which is taken from the prefaces of the books. All inquiries relating to the purchase of any book reviewed in these columns should be addressed to the publisher of the book in question.

Economy Loading of Power Plants and Electric Systems. By M. J. Steinberg and T. H. Smith. John Wiley and Sons, New York, N. Y.; Chapman and Hall, London, England, 1943. 203 pages, illustrations, etc., $9^1/_2$ by 6 inches, cloth, $3.50. (ESL).

The problem of allocation of load to power plants and to the equipment within them so as to produce electricity at the lowest cost consistent with continuity of service has become important as large electric systems have been interconnected. In this book the problem is discussed in detail. The underlying theory of economy loading is explained, the mathematical conditions for obtaining maximum efficiency are derived, the application of incremental rates for the solution of load-division problems is set forth, the limitations in the application of the theory are discussed, and the practical solution of load-division problems is explained. There is a bibliography.

Proteins, Amino Acids and Peptides as Ions and Dipolar Ions. (American Chemical Society Monograph Series No. 90.) By E. J. Cohn and J. T. Edsall, including chapters by J. G. Kirkwood, H. Mueller, J. L. Oncley, and G. Scatchard. Reinhold Publishing Corporation, New York, N. Y., 1943. 686 pages, illustrations, etc., $9^1/_2$ by 6 inches, cloth, $13.50. (ESL).

This book is the result of many years of study of an important topic on the border line between chemistry and biology. The evidence concerning the size and shape of molecules of the amino acids, peptides, and proteins is examined, and the number and distribution of the electric charges that they bear is examined and presented with full documentation. Especially, the authors consider the implications of the charged structure of these molecules for their physical properties, and their physicochemical interaction with other molecules.

ASTM Standards on Electrical Insulating Materials. The American Society for Testing Materials, Philadelphia, Pa., 1942. 439 pages, 6 by 9, $2.50. (ESL).

This edition of an annual publication contains 18 standard methods of testing insulating materials, 25 tentative test methods, and 16 specifications for insulating materials. The test methods are grouped under varnishes, paints, lacquers, and their products, plates, tubes and rods, molded materials, mineral oils, ceramic products, insulating paper, mica products, textile materials, and conditioning. Specifications are given for paint products, molded materials, plates, tubes and rods, rubber products, and textile materials. The annual report of the committee on electrical insulating materials, and several condensed reports on significance of tests are also included.

ASTM Standards on Rubber Products. Prepared by ASTM committee D-11 on rubber products. Methods of Testing Specifications. American Society for Testing Materials, Philadelphia, Pa., 1943. 301 pages, illustrations, etc., 9 by 6 inches, paper, $1.75. (ESL).

This annual brings together the standard and tentative methods of tests and specifications for rubber products which the Society has approved, together with a useful bibliography on rubber testing, thus forming a convenient reference book for the laboratory. General methods, and special ones for hose, belting, gloves, matting, tape, latex, cements, sponge and hard rubbers, and for insulated wire and cables are given. Emergency alternate provisions for various standard specifications are included.

ASTM Standards on Electrical-Heating and Resistance Alloys. Prepared by ASTM committee B-4. American Society for Testing Materials, Philadelphia, Pa., 1942. 160 pages, $1.50 per single copy, $1.25 per copy for orders of 10 to 49 copies.

This 1942–43 issue contains twenty-three specifications and tests for electrical heating and resistance alloys as well as four relevant technical papers. Two, on thermostat materials, are reprinted from the 1941 edition. The others are devoted to electrical surge tests on contact materials and testing machines. More standards were compiled for radio tube and lamp materials than for any other single category.

Practical Marine Diesel Engineering. By L. R. Ford. 4th edition. Simmons-Boardman Publishing Corporation, New York, N. Y., 1943. 642 pages, illustrations, etc., $9^{1}/_{2}$ by 6 inches, cloth, $6. (ESL).

The construction, operation, and maintenance of marine Diesel engines are explained thoroughly from the point of view of the operating engineer. The work is limited to the makes of engines that are being most used in the types of vessels now being built, especially those developed by the United States Maritime Commission. Other features are discussions of Diesel tugs, of deck and electrical machinery and of shipyard engine repairs. A chapter on license requirements is included.

Chemical Aspects of Light. By E. J. Bowen. Oxford University Press, New York, N. Y., Clarendon Press, Oxford, England, 1942. 191 pages, diagrams, etc., 9 by $5^{1}/_{2}$ inches, cloth, $4. (ESL).

Readers who wish to know something of the theories of light and of its interactions with atoms and molecules, but who are not thoroughly versed in mathematics, will find this a helpful supplement to more formal treatises. Among the matters treated are atomic and molecular spectra from the chemical point of view, the interrelations of scattering, Raman and fluorescence radiations, luminescence and phosphorescence, photochemical reactions, chemiluminescence and photocells.

The American Leonardo, a Life of Samuel F. B. Morse. By C. Mabee. Alfred A. Knopf, New York, N. Y., 1943. 420 pages, index I–XV, illustrations, etc., $9^{1}/_{2}$ by 6 inches, cloth, $5. (ESL).

Morse's career was outstanding in several fields. He was a portrait painter of distinction and founder of the National Academy of Design. He invented the telegraph and promoted it successfully. His life was a long, busy one, crowned with many honors. Mr. Mabee's biography, based on long study and access to family papers, gives a very satisfactory and interesting account of the man and his work, the most complete that has appeared.

Air Conditioning Analysis. By W. Goodman. The Macmillan Company, New York, N. Y., 1943. 455 pages, diagrams, etc., $9^{1}/_{2}$ by 6 inches, cloth, $6. (ESL).

The aim here is to present a comprehensive unified treatment of the fundamentals of the art of changing the condition of air, without discussing such related subjects as refrigeration, air handling, and temperature control. The material presented is developed from an elementary point of view and illustrated by numerous solved problems. Psychrometric charts with co-ordinates of enthalpy and specific humidity for a wide range of temperatures are included.

Empirical Equations and Nomography. By D. S. Davis. McGraw-Hill Book Company, New York, N. Y., and London, England, 1943. 200 pages, diagrams, etc., $9^{1}/_{2}$ by 6 inches, cloth, $2.50. (ESL).

The first part of this book describes practical methods for correlating engineering data and deriving usable empirical formulas. The fundamental methods of rectification are explained together with new techniques for two-variable data and a method of correlation for equations with three variables. Part two gives an excellent account of the theory and construction of nomographic and line co-ordinate charts. A good bibliography is included.

Industrial Radiology. By A. St. John and H. R. Isenburger. 2nd edition. John Wiley and Sons. New York, N. Y.; Chapman and Hall, London, England, 1943. 298 pages, illustrations, etc., $9^{1}/_{2}$ by 6 inches, $4. (ESL).

This book is intended to furnish in readable form authoritative information on the practical use of radiology in industry. The general principles governing the production and use of X rays and gamma rays are presented, together with the techniques suitable for important classes of industrial materials. A bibliography of over 1,300 titles is a valuable addition to the book.

Tennessee Valley Authority. By J. S. Ransmeier. Vanderbilt University Press, Nashville, Tenn., 1942. 486 pages, diagrams, etc., 9 by $5^{1}/_{2}$ inches, cloth, $3. (ESL).

The aim of this study is to contribute to clarification of the problem of cost allocation when river control is undertaken for multiple purposes. The author studies the problem as presented by the Tennessee Valley Authority. The development of the program is presented, the various theories and problems of cost allocation are discussed critically, and the planning and policy examined.

ASTM Standards on Copper and Copper Alloys. The American Society for Testing Materials, Philadelphia, Pa., 1942. 376 pages, 6 by 9 inches, $2.25.

In addition to more than 85 standards this current edition contains emergency specifications and emergency alternative provisions to the standards applying to copper alloy products. These are expected to aid in expediting procurement of materials. The specifications listed cover copper and copper alloy wires for electrical conductors and non-ferrous metals used in copper alloys as well as copper and copper alloys, cast and wrought.

Mechanics of Materials. By S. G. George and E. W. Rettger, revised by E. V. Howell. 2nd edition. McGraw-Hill Book Company, New York, N. Y., and London, England, 1943. 491 pages, diagrams, etc., $9^{1}/_{2}$ by 6 inches, cloth, $3.75. (ESL).

A simple complete account of the essentials of the subject is provided, suitable for use as a college text, but containing more material than is usually covered in an elementary course. In this edition the sequence of chapters has been altered, rivet and column specifications have been revised, and an article has been added on the graphical solution of combined stresses.

A Course in Radio Fundamentals. By G. Grammer. American Radio Relay League, West Hartford, Conn., 1942. 103 pages, illustrations, etc., $9^{1}/_{2}$ by $6^{1}/_{2}$ inches, paper, 50 cents. (ESL).

This is a study guide containing examination questions and laboratory experiments which is intended to be used with the Radio Amateur's Handbook. The course is intended primarily for self instruction, but will also be of interest to teachers. The apparatus required is simple and can usually be constructed with material that the amateur has at hand.

Abridged Scientific Publications from the Kodak Research Laboratories. Volume XXIII, 1941. Eastman Kodak Company, Rochester, N. Y., 1942. 283 pages, $6^{1}/_{2}$ by $9^{1}/_{4}$ inches.

This is a collection of thirty-five papers published by members of the laboratory staff in various technical journals. The papers presented here are shortened and the experiments described cover many facets of the photographic process.

PAMPHLETS • • •

The following recently issued pamphlets may be of interest to readers of "Electrical Engineering." All inquiries should be addressed to the issuers.

Construction, Maintenance and Repairing of Aircraft Instruments. By J. A. Gordon. Reprinted by Henry Paulson and Company, 30 South Wabash Avenue, Chicago, Ill., from the *Horological Journal*, London, England, 1943. 29 pages, no charge.

Technique of Molding Low Loss Phenolics. Bakelite Corporation, 30 East 42d Street, New York, N. Y., 1943. 16 pages, no charge.

Tube Picture Book. RCA Victor Division of the Radio Corporation of America, Harrison, New Jersey, 1943. 7 pages, 10 cents. Sale restricted to United States and Canada.

Report From the Advisory Committee on Government Questionnaires. United States Chamber of Commerce, 1615 H Street, N. W., Washington, D. C., 1943. 61 pages.

Manpower Lost. War Manpower Commission, 11 West 42d Street, New York, N. Y., 1943. 28 pages.

Absenteeism. War Production Board, Washington, D. C., 1943. 27 pages.

Controlling Absenteeism. United States Department of Labor, Washington, D. C., 1943. 57 pages, no charge.

A Study of Glaze Stresses. By William C. Bell. Ohio State University, Columbus, 1943. 28 pages, 40 cents.

The Foreman. National Association of Manufacturers, 14 West 49th Street, New York, N. Y., 1943. 16 pages, no charge for single copies; quantities at cost.

TRANSACTIONS SECTION

Preprint of Corresponding Pages From the Current Annual AIEE Transactions Volume

Any discussion of these papers will appear in the June 1943 Supplement to Electrical Engineering—Transactions Section

Performance Calculations on Tapped-Winding Capacitor Motors

A Method of Calculating Low-Speed Performance of Two-Speed Capacitor Motors With Open-End Type of Control Winding

P. H. TRICKEY
MEMBER AIEE

THE tapped-winding capacitor motor is used quite commonly to obtain a two-speed fan motor with a comparatively simple switching arrangement. With the open-end type of connection as shown in Figure 1, the high-speed connection gives a normal capacitor motor, and its performance under any load may be determined by calculation with the formulas of either the revolving-field theory or the cross-field theory. When a control winding is added to the already complicated capacitor motor circuits, the analysis by the usual methods becomes even more difficult, and the calculation more involved and tedious than ever. In the following discussion a method is presented for calculating the low-speed performance. This method while not absolutely rigorous, should give results well within the limits of variation of individual motors of the same design.

Analyzing the Problem

The problem may be divided into two parts:

1. The performance of the motor is obtained when operating with the high-speed connection at the lower than normal voltage which will give the desired second speed.

2. If one knows the performance at the proper speed with a certain line voltage applied, the voltage is calculated which will be necessary to apply to the total main and control winding to give the same speed. If this voltage is not equal to the line voltage, a different control winding must be designed

Low-Speed Performance on High-Speed Connection

One procedure to obtain the low-voltage performance would be to calculate the full-voltage performance at the desired second speed such as point *B* in Figure 4. Then if one knows that the fan torque of point *A* varies as the square of the speed and that the motor torque of point *B* varies as the square of the voltage, the proper voltage to use in calculating point could be obtained by simple ratios.

In actual practice, however, this procedure does not always work out so perfectly, partly because of the change in saturation factor with lower voltage, but mainly because the friction does not drop off with voltage, the torque which should equal point *C* very often is a little more or less, and a third calculation must be made. It has been found advantageous to estimate the voltage for point *C* with its corresponding saturation factor. It is usually found that the torque is so close to point *C* that in one more trial the performance at point *C* is determined, and thus the result is obtained in two trials instead of three.

Another and sometimes more convenient method to obtain the low-voltage low-speed performance is by actual test. The common fan-load test with variable voltage known as the fan saturation test gives the performance of the motor and blade at various voltages and speeds. It makes no difference to the second part of this problem whether the low-voltage low-speed values are obtained from test or calculation. In fact, the calculation sheet shown in Figure 3 shows an approximate method for use when the test does not have phase readings.

Low-Speed Performance on Low-Speed Connection

With the low-voltage performance as a foundation, it is now possible to calculate the performance on the low-speed connection at the same rpm. With the speed set, the voltage necessary to drive the fan at this speed is calculated, and the current and input are obtained. If this voltage

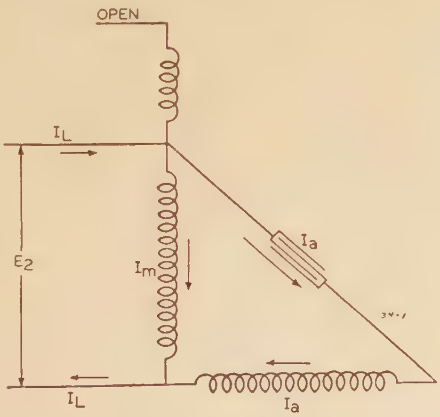


Figure 1. Low-voltage operation on high-speed connection

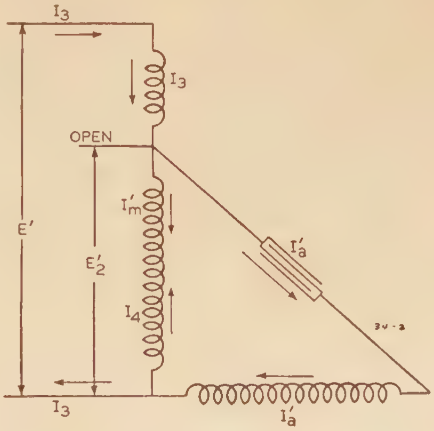
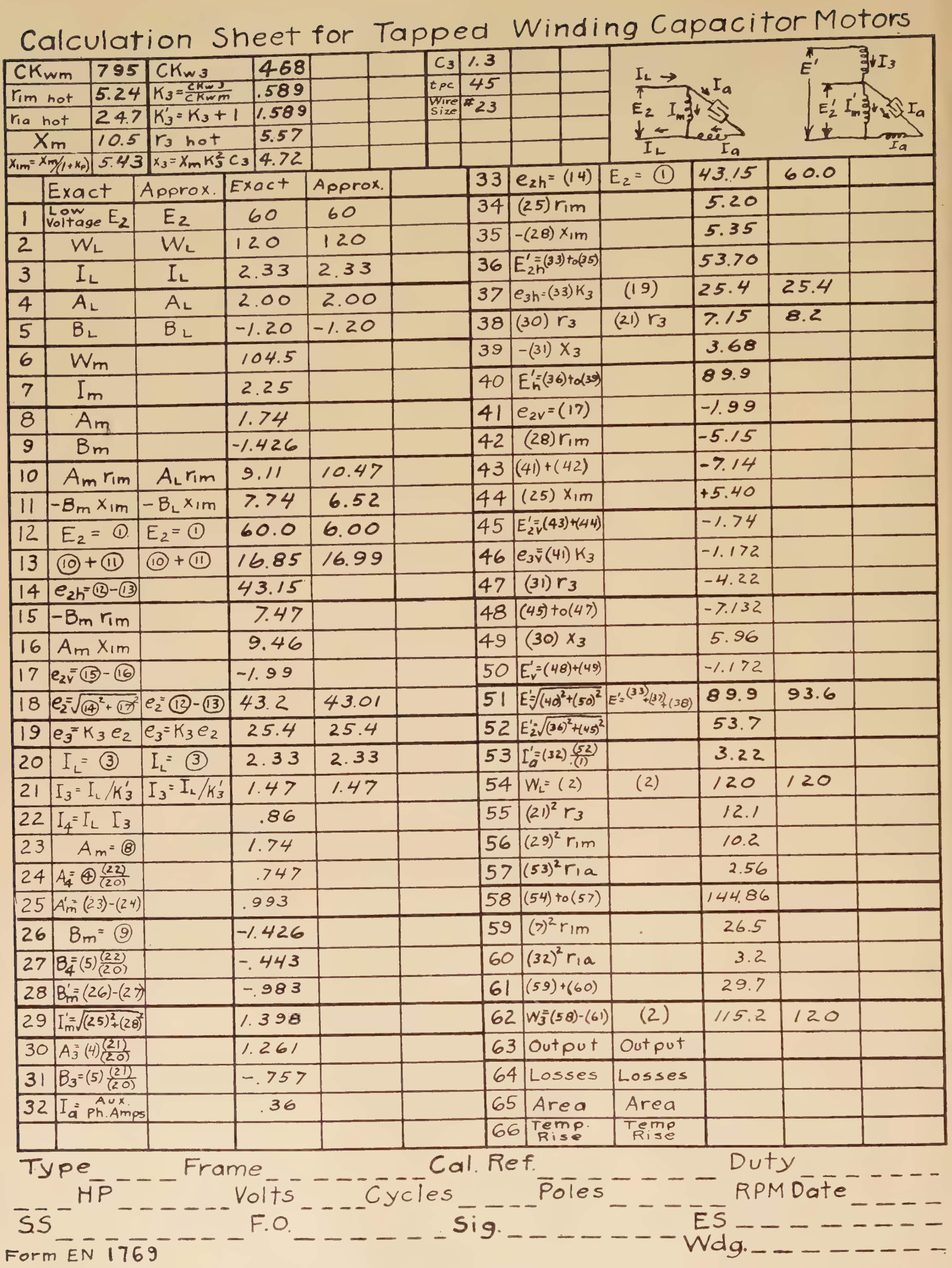


Figure 2. Full-voltage operation on low-speed connection

Paper 43-4, recommended by the AIEE committee on electrical machinery for presentation at the AIEE national technical meeting, New York, N. Y. January 25–29, 1943. Manuscript submitted June 4, 1942; made available for printing October 20, 1942.

P. H. Trickey is chief engineer, Diehl Manufacturing Company, Elizabethport, N. J.


I_L
I_a
E_2
I_m
I_L
I_a
E'
I_3
E'_2
I'_m
I_a
I_a

Calculation Sheet for Tapped Winding Capacitor Motors

CK_{wm}	795	CK_{w3}	468			C_3	1.3		
r_{im} hot	5.24	$K_3 = \frac{CK_{w3}}{CK_{wm}}$	.589			tpc	45		
r_{ia} hot	24.7	$K'_3 = K_3 + 1$	1.589			Wire Size	#23		
X_m	10.5	r_3 hot	5.57						
$X_{im} = X_m/(1+K_p)$	5.43	$x_3 = X_m K_3^2 C_3$	4.72						

	Exact	Approx.	Exact	Approx.	
1	Low Voltage E_2	E_2	60	60	
2	W_L	W_L	120	120	
3	I_L	I_L	2.33	2.33	
4	A_L	A_L	2.00	2.00	
5	B_L	B_L	-1.20	-1.20	
6	W_m		104.5		
7	I_m		2.25		
8	A_m		1.74		
9	B_m		-1.426		
10	$A_m r_{im}$	$A_L r_{im}$	9.11	10.47	
11	$-B_m x_{im}$	$-B_L x_{im}$	7.74	6.52	
12	$E_2 =$ ①	$E_2 =$ ①	60.0	6.00	
13	⑩ + ⑪	⑩ + ⑪	16.85	16.99	
14	$e_{2h} =$ ⑫ − ⑬		43.15		
15	$-B_m r_{im}$		7.47		
16	$A_m X_{im}$		9.46		
17	$e_{2v} =$ ⑮ − ⑯		-1.99		
18	$e_2 = \sqrt{⑭^2 + ⑰^2}$	$e_2 =$ ⑫ − ⑬	43.2	43.01	
19	$e_3 = K_3 e_2$	$e_3 = K_3 e_2$	25.4	25.4	
20	$I_L =$ ③	$I_L =$ ③	2.33	2.33	
21	$I_3 = I_L / K'_3$	$I_3 = I_L / K'_3$	1.47	1.47	
22	$I_4 = I_L \quad I_3$		.86		
23	$A_m =$ ⑧		1.74		
24	$A_4 =$ ④ $\frac{(22)}{(20)}$		.747		
25	$A'_m = (23) - (24)$		.993		
26	$B_m =$ ⑨		-1.426		
27	$B_4 = (5)\frac{(22)}{(20)}$		-.443		
28	$B'_m = (26) - (27)$		-.983		
29	$I'_m = \sqrt{(25)^2 + (28)^2}$		1.398		
30	$A_3 = (4)\frac{(21)}{(20)}$		1.261		
31	$B_3 = (5)\frac{(21)}{(20)}$		-.757		
32	$I_a =$ Aux. Ph. Amps		.36		

33	$e_{2h} = (14)$	$E_2 =$ ①	43.15	60.0	
34	$(25) r_{im}$		5.20		
35	$-(28) x_{im}$		5.35		
36	$E'_{2h} = (33)$ to (35)		53.70		
37	$e_{3h} = (33) K_3$	(19)	25.4	25.4	
38	$(30) r_3$	$(21) r_3$	7.15	8.2	
39	$-(31) X_3$		3.68		
40	$E'_h = (36)$ to (39)		89.9		
41	$e_{2v} = (17)$		-1.99		
42	$(28) r_{im}$		-5.15		
43	$(41) + (42)$		-7.14		
44	$(25) X_{im}$		+5.40		
45	$E'_{2v} = (43) + (44)$		-1.74		
46	$e_{3v} = (41) K_3$		-1.172		
47	$(31) r_3$		-4.22		
48	(45) to (47)		-7.132		
49	$(30) x_3$		5.96		
50	$E'_v = (48) + (49)$		-1.172		
51	$E' = \sqrt{(40)^2 + (50)^2}$	$E' = (33) + (37) + (38)$	89.9	93.6	
52	$E'_2 = \sqrt{(36)^2 + (45)^2}$		53.7		
53	$I'_a = (32)\frac{(52)}{(1)}$		3.22		
54	$W_L = (2)$	(2)	120	120	
55	$(21)^2 r_3$		12.1		
56	$(29)^2 r_{im}$		10.2		
57	$(53)^2 r_{ia}$		2.56		
58	(54) to (57)		144.86		
59	$(7)^2 r_{im}$		26.5		
60	$(32)^2 r_{ia}$		3.2		
61	$(59) + (60)$		29.7		
62	$W_3 = (58) - (61)$	(2)	115.2	120	
63	Output	Output			
64	Losses	Losses			
65	Area	Area			
66	Temp. Rise	Temp Rise			

Type____ Frame________ Cal. Ref.________ Duty_______
___HP_______ Volts____ Cycles____ Poles______ RPM Date_____
SS____________ F.O._________ Sig.__________ ES__________
Form EN 1769 Wdg.___________

Figure 3. Example calculation

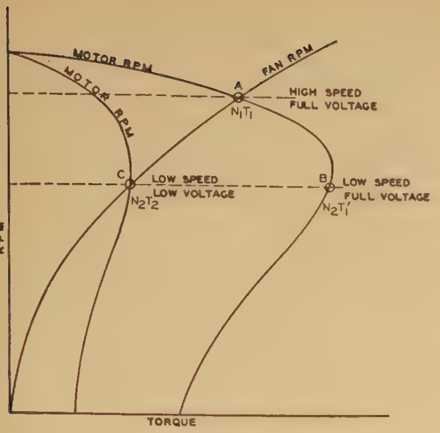


Figure 4. Motor and fan speed-torque curves

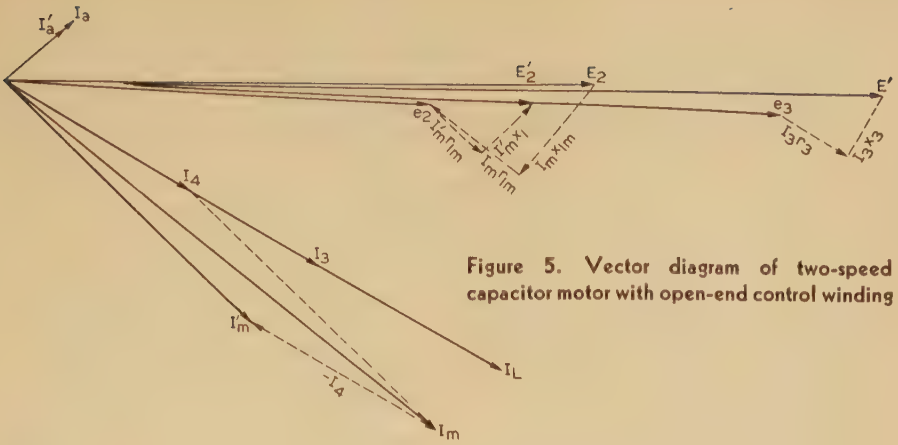


Figure 5. Vector diagram of two-speed capacitor motor with open-end control winding

E' is not equal to the line voltage, the motor will not drive the fan at the proper second speed, and a new control winding must be designed.

Figure 5 shows the combined vector diagrams of the two connections when operating at the same speed. I_m, I_a, and I_L are the currents with the high-speed connection and low-voltage E_2. The induced voltage e_2 in the main winding is obtained by subtracting the reactance and resistance drops in the main winding due to the current I_m. It is assumed that this induced voltage will remain the same under the low-speed connection if the speed is maintained at the same value.

Since the control winding is placed in the main-winding slots, it acts with the main winding to form an autotransformer as well as to provide a series impedance. It is assumed that the autotransformer and motor effects are superimposed on each and that the motor provides the magnetizing current. Because of the transformer action, the new line amperes I_3 will be reduced from I_L in the transformer ratio.

$$I_3 = I_L/K_3'$$

where

$K_3' = K_3 + 1$

$K_3 = CK_{w3}/CK_{wm}$

CK_{w3} = effective series conductors in control winding

CK_{wm} = effective series conductors in main winding

Because of the transformer action, the main winding carries a transformer current

$$I_4 = I_L - I_3$$

The actual current in the main winding I_m' will be a combination of this current I_4 with the main-winding motor current I_m.

$$I_m' = I_m - I_4$$

When all the new current values in each branch of the circuit are obtained, the new low-speed connection line voltage E' may be obtained by adding to e_2 the proper impedance drops plus the induced voltage e_3 in the control winding.

$$E' = e_2 + I_m' r_{1m} + jI_m' x_{1m} + e_3 + I_3 r_3 + jI_3 x_3$$
$$e_3 = K_3 e_2$$

The input will differ somewhat from W_L, the input on the high-speed connection, by the addition of the control winding copper loss $I_3^2 r_3$, and by the reduction in the copper loss in the other two windings. The main-winding copper loss is reduced because of the lower value of the current I_m'. Also due to this reduced main-winding current, the capacitor-phase terminal voltage E_2' is less than E_2, and with it the capacitor-phase current I_a'. The capacitor-phase copper loss is reduced accordingly.

Figure 3 shows a convenient form of calculation sheet with an illustrative example.

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Analysis of Rating Methods for Intermittent Loads

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Synopsis: The economics of rating structures for electrical equipment from the viewpoints of manufacturing and application are considered. Methods for dealing with periodic load applications are reviewed. The present methods have certain limitations for some practical applications. An improved method for applying motors to load cycles where the motor stops and ventilation changes in the no-load part of the cycle is developed. This method is based on certain motor-loss ratios, that is, the relation between the losses dissipated with standard temperature rise running and at standstill and the losses at the nominal or short-time rating. It permits the application of service-factor or short-time ratings to periodic loads with more exactness. Attention is called to the wide variations in permissible rms current on periodic loads for different loss ratios and changes in ventilation on a motor.

Rating Structures

RATINGS for intermittent or varying loads have recently received considerable attention[4–10] and AIEE Standard 1*A*[1] is an attempt to clarify and rationalize suitable methods for this purpose. No radically new methods of rating are proposed, but some new nomenclature and new viewpoints are introduced; the report also advocates increased use of some methods of rating which in the past have been applied to only a limited extent.

Naturally there is some hesitation in making changes in the rating practice, which is justified, because departures from well-established methods are likely to lead to confusion and expense. On the other hand, it must be realized that any change in practice which results in economies appreciably outweighing the disadvantages involved will probably be made sooner or later. All action of this nature should be based primarily on economic considerations. Broadly speaking, the number of rating methods should be kept as small as possible but they should not be so limited that they prevent satisfactory and reasonably economical applications of devices under the various conditions encountered in practice.

In making changes in the rating structure from an economic point of view, serious thought should be given to what such a change really entails. If a new rating method requires entirely new lines of machines and apparatus different from those already in existence and thus interferes with quantity production, and if it furthermore complicates the stock situation both at the factory and in the field, it is justified only if it leads to decided improvements. On the other hand, if a change merely means giving some additional data for apparatus already standardized, it should not be objectionable. Even here it must be realized that the additional information means expense and complications in listing, merchandising, and applying the apparatus. Hence the inclination to adhere to the present practice of handling most apparatus by means of a single continuous rating as far as possible is quite natural. In the past, a departure from this simple practice has in general been made only under compelling circumstances. An example of an extreme case is the rating and application of single-phase railway motors[2] where the information given with the rating structure entails weeks of expensive tests as well as time-consuming methods of application. These expenses, however, are unquestionably well justified in view of the large number of motors of the same rating in use and the enormous investment involved. Between the extreme conditions there is, of course, room for a variety of intermediate practices, some of which have been used to a limited extent, as, for example, the rating structure for d-c railway, mill-type, and other motors. In deciding whether the use of a more complicated rating structure is warranted, the expense for securing and giving additional rating data as well as the advantages derived should be taken into account. Frequently the inclusion of additional information will result in easy and economical application and fully compensate for the expense involved. The choice of method should also be influenced by the existence or lack of well-established practices. If a rating practice has been in use for many years and has given reasonable satisfaction, a change would seem advisable only if the advantages obtained are appreciable. On the other hand, where it is necessary to establish new rating structures in entirely new fields, it seems logical to apply newer methods although they have only moderate advantage over others. In the following it is proposed to discuss some of the methods recently advocated and to point out their limitations; subsequently methods are indicated for overcoming these limitations and for handling cases which do not seem to be covered satisfactorily by AIEE Standard 1*A* and related literature.

Ratings for Intermittent Duty

AIEE Standard 1*A* strongly emphasizes the many applications of electrical machines and apparatus where a given load is carried for sustained periods or repeatedly for shorter periods, but where there are also light- or no-load conditions. The device must be satisfactory for the required load in many respects, such as regulation, torque characteristics, mechanical strength, and so forth, and it is therefore proposed to assign a nominal rating corresponding to the load. It is also proposed to take advantage in the design of the noncontinuous nature of the load.

Two methods are given for such cases, namely, the dual or service-factor method and the load-cycle method of rating. In the first method the nominal rating is supplemented by a service factor, which is the ratio of the continuous rating to the nominal rating and thus indicates the continuous rms rating of the machine or apparatus. This method, when applicable, has the advantage of being much more flexible in application than the load-cycle method. It permits the analysis of loads of varying size and time of application, merely requiring that the rms load does not exceed the continuous load indicated by the service factor. In contrast, the load-cycle method is rather inflexible in its present form as its application is limited to a single load cycle, with both the amplitude and duration of the load for each cycle specified. Therefore, this method naturally will be considered only where for some reason the service-factor method is not suitable.

Either of the periodic rating methods permits designs smaller than those with continuous ratings equal to the proposed nominal ratings of the periodic rating methods. Thus appreciable economy

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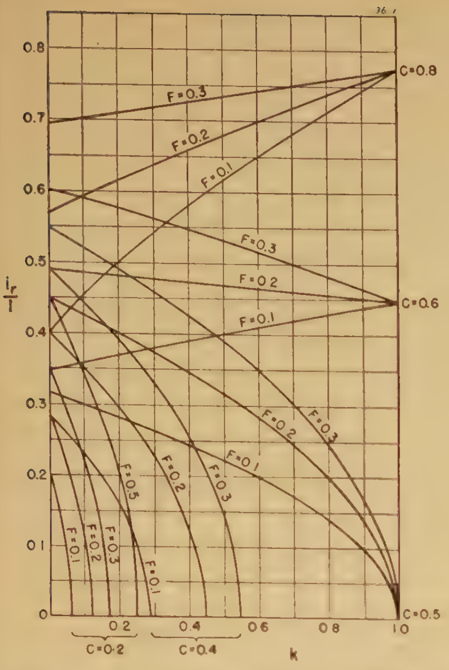

Figure 1. Curves for equation 3 of Appendix I, with loss ratios C and F variable and $p=0.5$, showing permissible rms currents for different ratios of running time per cycle

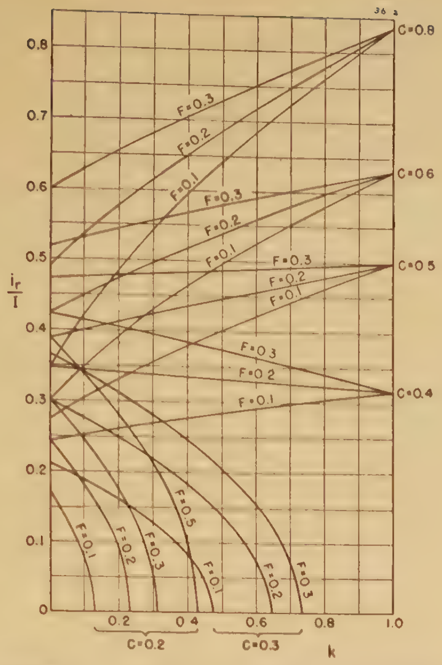

Figure 2. Curves for equation 3 of Appendix I, with loss ratios C and F variable and $p=0.333$, showing permissible rms currents for different ratios of running time per cycle

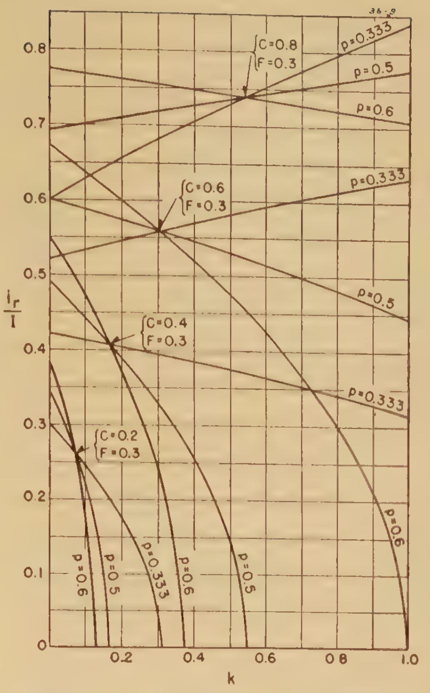

Figure 3. Curves for equation 3 of Appendix I, with loss ratios p and C variable and $F=0.3$, showing permissible rms currents for different ratios of running time per cycle

may be accomplished in view of the many applications where continuous loads are not involved. However, it is highly desirable to obtain this economy without creating entirely new lines of ratings. This can be done by using the standard short-time ratings[5,8] wherever they exist. These ratings cannot carry the nominal rated load continuously but usually are suitable for service-factor or load-cycle ratings. To get a service-factor rating, or to indicate the load duration and the percentage of the total time during which the nominal load can be carried to obtain a load-cycle rating, it is then merely necessary to supplement the short-time or nominal rating with the service factor. Even where no short-time ratings are available and new ratings have to be established, it seems desirable to make designs which are suitable for two or more of the rating methods, such as short-time, service-factor, and load-cycle ratings.

Unfortunately the service-factor method, which is flexible in its application to a great variety of load conditions, is not always applicable. It is based on a single rms value applying for a constant condition of cooling, which results in limitations under certain conditions.

For example, when a device cannot carry any load continuously without overheating, it is obviously impossible to give a service factor. This is frequently the case with enclosed machines or other enclosed apparatus where the core and similar losses, if prevailing continuously, cause temperature rises in excess of those permissible for continuous operation. Such devices can be and frequently are used to good advantage on intermittent loads where no core or similar losses occur during idle periods. (In a few cases, such as series d-c motors where the speed varies and the operating voltage ranges between zero and rated voltage, an rms rating can at times be given at a reduced voltage corresponding to the average operating voltage and a modified application of the service-factor method is possible.)

Again, when the ventilation during the on and off periods of a load cycle changes materially, making the resultant effective ventilation appreciably less than the ventilation prevailing during the tests with the continuous load current, the service-factor method cannot be readily applied. Motors which stand still during the no-load periods, with an appreciable reduction in ventilation, are an illustration of this.

When the heating or other operating limitations of devices are not entirely a function of the rms current, the service-factor method is not very suitable. For example, in electronic tubes the safe output is influenced by many different factors but only to a negligible extent by the rms load, and therefore the rms load cannot be used as a correct indicator of permissible load.

In these cases and possibly others, the load-cycle method of rating may have to be considered, and it would be of advantage to find ways of making this method more flexible. One possible way is to assign to a given device, in place of a single load-cycle rating, a number of different load-cycle ratings[9,10] in such a way that intermediate applications can be handled by interpolation. This method has been used with electronic devices for applications, such as spot welding, and is giving satisfactory results in practice. Although the assignment of a number of ratings naturally introduces complications, in this case the necessary interpolation can be readily and quickly made and, in fact, takes less time than the calculation of the rms or average currents. In other words, the extra expense for establishing several ratings is compensated for by time saved in applying the device.

Periodic Loads on Motors

Unfortunately, a similar practice for motors is almost out of the question. An acceptance test for a load cycle during which the motor must be started and stopped repeatedly is difficult because of temperature measurement and the necessity of properly adjusting the load to the desired value during each cycle. Fair results can be obtained where the operating period is reasonably long, but serious difficulties are encountered in short load cycles, which are more common in practice. Therefore an entirely different method of attack seems desirable. In a previous paper[7], a method permitting certain load-cycle tests with relatively long operating periods on standard short-time rating motors was outlined. Methods were indicated whereby data obtained from such tests could be utilized to derive results for load cycles of short duration. In the following a simpler method applicable with short load cycles is suggested.

The Loss-Ratio Method

Basically, the temperature rise of a motor is determined by the losses and the ability of the motor to dispose of them. This ability is obviously much different when the motor is at rest than when running. Consequently it seems almost hopeless to devise proper methods of application without some knowledge of the losses and the ability of the motor to dispose of heat during each of the widely different conditions of operation. Furthermore, with this knowledge, applications can be readily made for any motor having a short-time or any other nominal rating. The essential quantities are:

1. The constant losses (core and so forth) at the nominal rating $=L_c$
2. The variable losses (copper and so forth) at the nominal rating $=L_v$
3. The continuous losses permissible for standard temperature rise[1] at rated speed $=L_r$
4. The continuous losses permissible for standard temperature rise at standstill $=L_s$

Values for the first two items are always available from the efficiency determination of the machine. With a service-factor rating, the value for the third item can be readily calculated from items 1 and 2 by use of the service factor (equation 4, Appendix I). If no service-factor rating is available, the information for item 3 can be obtained from a continuous run at reduced voltage and load giving standard temperature rise. Preferably the ratio between constant and varying losses during this test should be the same as at the nominal rating. The information for the fourth item can be obtained by a continuous test run with the motor at rest, at a voltage low enough to again result in the standard temperature rise. Then by means of the simple algebra of Appendix I, any desired load-cycle condition can be calculated. Formula 2 gives the load current which can be carried for any value of running time per cycle, and formula 3 indicates the rms current for the entire load cycle corresponding to the load current of formula 2. Figure 1 shows curves indicating the value of the permissible rms current for cases where the constant losses and varying losses are equal at the nominal rating ($p=0.5$). Figure 2 shows similar curves for applications where the constant losses are one third and the varying losses two thirds of the total losses at the nominal rating ($p=0.33$). It will be noted that the permissible rms current for the different ratios varies over very wide ranges, which confirms the fact that inexact methods are not likely to give very satisfactory results. Also, it is evident that the basic factors considered in the present method should be known to make reasonably accurate calculations possible.

Figure 3 again shows values for the rms current for a constant ratio of permissible continuous losses standing and running ($F=0.3$). Different ratios (p) for constant to total losses at nominal rating and different ratios (C) for permissible continuous losses running to total losses at the nominal rating are used. A number of curves of this character, each for a different value of F, seem to be most suitable for practical use. The ratio F is a machine characteristic, whereas p and C depend upon the nominal rating considered. Reasonably correct results can be obtained from such curves by interpolation for different values of the various ratios. Since the variables used in this method are all ratios of losses, this method might be referred to as the "loss-ratio method." It is applicable directly to existing short-time ratings as well as to service-factor ratings, and it therefore obviates the necessity for separate and distinct lines of additional ratings.

In a previous paper[8] dealing with load-cycle ratings, a factor (S_3) was introduced to cover motors standing still during no-load periods. The formulas given were based on a factor (f), intended to make allowance for reduced ventilation and the absence of core losses at standstill. These two effects influence results in opposite directions, and thus factor f depends upon the difference of two quantities. Since in practice each of these effects varies over wide ranges, the resultant difference is erratic, making it almost impossible to deal with uniformly consistent values for f (see discussion of paper, reference 8). Formula 5 of Appendix I gives the same factor (S_3) in terms of the loss ratio F. With the method now proposed, factor F depends only upon the change of ventilating conditions and hence is likely to be fairly uniform, or at least quite consistent for a given type and line of machines. Therefore the present method will yield reasonably correct results even if test data for the value F are not available for all sizes of a line of motors.

The calculations outlined above deal with the total losses and neglect the fact that hot-spot temperatures are influenced to some extent by the distribution of the losses in various parts of the machine. This can be taken into account by allowing a slight factor of safety in applying the motors. It is also evident that the previous considerations apply more particularly to a-c induction motors, the type most universally used. For the different conditions prevailing in commutator-type motors, certain modifications will be necessary; for series motors in particular, more complicated methods of application such as used for d-c railway motors may be in order.

These calculations apply directly to applications where the load cycles are rather short and where the temperature of the machine does not vary materially during the cycle, the applications most common in practice. For relatively long load cycles or for load cycles very unevenly distributed, the methods previously outlined[8] for the service-factor method can be used, or the factor S_2 given there can be applied to the method outlined here. In other words, the factor S_2 can be used to give the relations between short- and long-cycle applications regardless of whether the service-factor rating is given. When no service factor is given, the permissible rms current is obtained from formula 3, Appendix I, for short-load cycles. By multiplying this value by the factor S_2 (Figure 7 of reference 8), permissible rms values for longer load cycles involving appreciable temperature changes can be obtained. Thus the loss-ratio method is applicable to both types of periodic duty cycles involving standstill during the no-load period, a condition for which other methods have limitations.

Appendix I. Determination of Permissible Periodic Load Cycles for Motors by the Loss-Ratio Method

Let

$L=$ total losses at the nominal rating current I
$L_c=$ constant (core and so forth) losses $=pL$
$L_v=$ variable (copper and so forth) losses $=(1-p)L$
$L_r=$ continuous losses at rated speed for standard temperature rise
$L_s=$ continuous losses at standstill for standard temperature rise

$L_r = CL$ where $C = L_r/L$
$L_s = FL_r = CFL$ where $F = L_s/L_r$
T = total time per cycle
$T_r = kT$ = running time per cycle

For any value of k, the losses which can be dissipated during the total cycle must equal the losses produced by a certain load current (i_k) during the running part of the cycle.

Therefore

$$L_rk + L_s(1-k) = L_ck + \left(\frac{i_k}{I}\right)^2 L_vk \qquad (1)$$

Introducing the loss ratios p, C, and F and solving

$$i_k = I\sqrt{\frac{1}{1-p}\left(C-p-CF+\frac{CF}{k}\right)} \qquad (2)$$

The rms current for the entire cycle corresponding to the periodic current (i_k) is

$$i_r = \sqrt{\overline{i_k{}^2k}} = I\sqrt{\frac{k}{1-p}\left(C-p-CF+\frac{CF}{k}\right)} \qquad (3)$$

For a motor with a service-factor rating, let

S_1 = service factor
i = continuous rating current
S_2 = ratio of rms current to continuous rating current

Then

$$L_r = pL + S_1^2(1-p)L \qquad (4)$$

Also

$$L_r = L_c + \left(\frac{i}{I}\right)^2 L_v$$

from which

$$i = I\sqrt{\frac{C-p}{1-p}}$$

Then

$$S_2 = i_r/i = \sqrt{\frac{k}{C-p}\left(C-p-CF+\frac{CF}{k}\right)} \qquad (5)$$

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A New Type of Adjustable-Speed Drive for A-C Systems

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Synopsis: This paper describes the theory and characteristics of a new type of polyphase adjustable-speed motor and its control circuit. The system provides an a-c drive that has characteristics comparable to those of the Ward Leonard system used on direct currents. The control circuit provides starting as well as speed adjustments from 0 to 100 per cent of rated value. The power factor of the motor is either leading or very close to unity over its operating range, and consequently the efficiency is high. Low costs of this type of motor and its control system, the large speed range, the facility for rapid reversal by regeneration, and its high starting torque make it suitable for some industrial applications.

ATTEMPTS to adapt d-c shunt motors for use on alternating currents have been unsuccessful because of the difficulty in producing a flux that is in time phase with the armature current. In order to obtain a high power factor and a large torque, such a motor would have its armature current under load inphase or approximately inphase with both the armature voltage and the air-gap flux. These conditions are unattainable in the a-c shunt motor. When the field and armature are supplied with the same voltage, the phase of the field current is approximately 90 degrees behind that of the voltage. The flux produced by the field is approximately in quadrature with the armature voltage. The armature current is more nearly inphase with the armature voltage, and consequently it cannot be inphase with the flux. In addition to this the generated voltage produced by rotation of the armature in the field is approximately 90 degrees out of phase with the applied voltage instead of being directly opposed to it as in a d-c motor. Consequently the armature current is limited largely by the impedance of the armature, and speed has little effect on this current. Thus the single-phase shunt motor used on alternating current will take very large currents at all loads, and, since changes in generated voltage have little effect on this armature current, increases in load will cause large reductions in speed. The speed regulation of such a motor is objectionably high.

The undesirable features referred to above can be overcome by adapting the motor for use on polyphase systems in a manner such that the voltage applied to the field is approximately at right angles in time phase with respect to the armature voltage. Under such conditions the inductive effect of the field will cause the air-gap flux to be approximately inphase with the armature voltage. The generated voltage in the armature due to rotation will oppose the applied voltage, causing the motor to behave very much like a d-c shunt machine. Thus in Figure 1, if the field and the armature of a motor are supplied from separate phases of a two-phase system, the motor would take a small armature current at no load and an increased current when load is applied. The armature current would be out of phase with the armature voltage, and, because of the large armature impedance, the speed regulation would be similar to that of a shunt motor with a large resistance in its armature circuit.

The Motor and Its Circuits

Objections to the two-phase motor shown in Figure 1 can be overcome by

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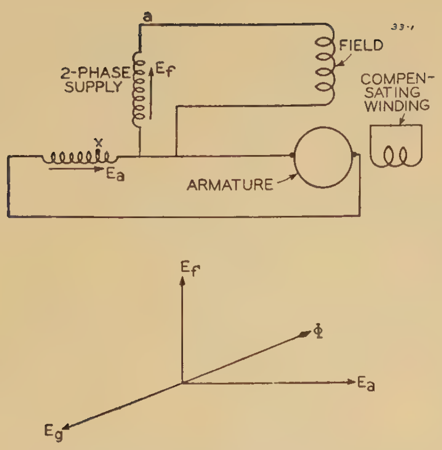

Figure 1. The motor supplied with two-phase voltages and its vector diagram

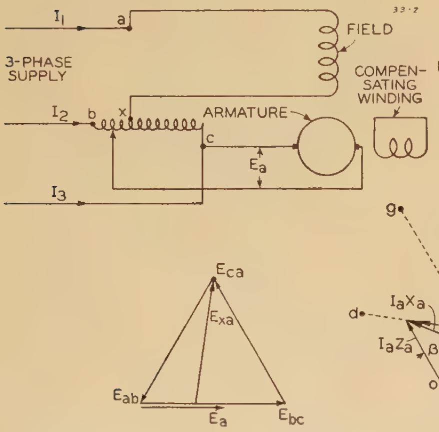

Figure 2. The motor supplied with three-phase voltage and the voltage vectors

proper design of the motor and by supplying the field with a voltage that is in proper phase relationship with respect to the armature voltage. The armature should be designed with the lowest value of impedance practicable. This requires a compensating winding on the stator which can be connected either in series with the armature or short-circuited—the latter connection produces inductive compensation. The voltage applied to the field should be shifted in time phase with respect to its quadrature position shown in Figure 1, so that the time phase of the air-gap flux is slightly behind the armature applied voltage. This shift is essential in order that the power factor of the motor be kept high and can be accomplished in the circuit shown in Figure 1 by connecting the field across the points a and x.

The methods of obtaining this slight shift of voltage on polyphase systems are numerous. The requisite voltages for field and armature referred to above can be obtained from a three-phase supply by use of the circuit shown in Figure 2. The motor has an armature similar to that of an ordinary repulsion motor. The compensating winding is placed on the stator in such a position as to neutralize the flux produced by the armature ampere turns. The main field winding is placed on the stator and is spaced 90 electrical degrees with respect to the compensating winding. Three-phase voltage is supplied at the points a, b, and c, and an adjustable transformer is connected between b and c. The motor armature can be supplied with all or any portion of the voltage between the points b and c. Selection of speed is obtained by adjustment of this armature voltage. The field winding is connected between line a and point x on the adjustable transformer. The field winding acts as a transformer in making essentially a three-phase to two-phase transformation of voltages. This differs from the ordinary Scott connection in that the point x is not exactly midway between b and c.

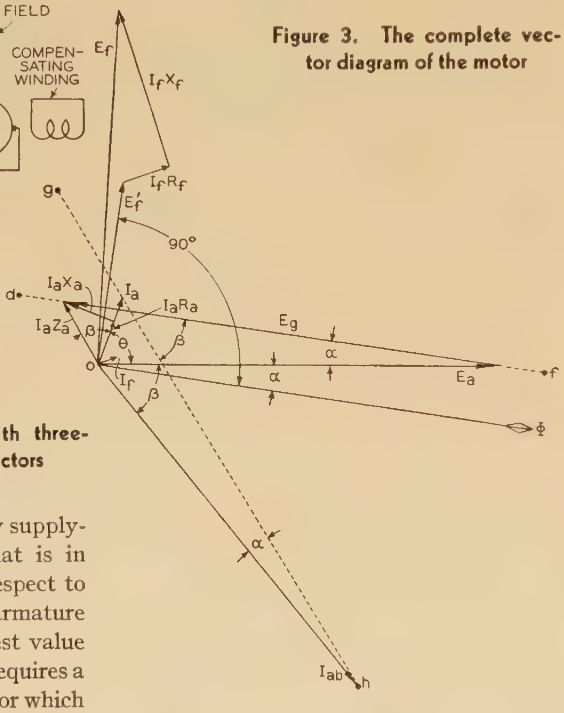

Figure 3. The complete vector diagram of the motor

Theory of the Motor

The theory and operation of the motor connected as in Figure 2 can be explained on the basis of its vector diagram shown in Figure 3. This is the diagram for a two-horsepower motor constructed in accordance with the required features described above. The field voltage is shown as the vector E_f, and for this particular case it is almost perpendicular to the armature voltage vector E_a. The phase angle between these voltages can be changed by moving the tap x on the control transformer. In the analysis presented here it is assumed that the armature and field voltages remain constant and fixed in direction for all values of load and that the impedance of the control transformer is negligible.

When voltage is supplied to the field of this motor, the back electromotive force in the field is produced by a resistance drop, a drop due to the mutual flux that cuts the armature and the field conductors, and a drop due to the leakage flux which cuts the field winding but not the armature. The vectors representing these voltages are shown in Figure 3. The voltage applied to the field is E_f, the leakage-reactance voltage drop is I_fX_f, and the voltage produced in the field winding by the mutual flux is E_f'. The mutual field flux that generates the voltage E_f' in the winding is represented by the vector ϕ.

If the armature voltage E_a is applied while the rotor is held stationary, the armature current will be limited by the equivalent resistance and reactance of the armature circuit. The equivalent resistance R_a is determined by the effective resistance of the armature circuit plus the resistance of the compensating winding reflected to the armature; whereas the equivalent reactance X_a is determined by the closeness of coupling of the compensating winding to the armature. Thus the blocked-rotor armature current I_{ab} is equal to the voltage E_a divided by the impedance Z_a of the armature circuit, where

$$Z_a=\sqrt{R_a^2+X_a^2}$$

The current I_{ab} shown in Figure 3, lags the armature voltage by the angle β where

$$\beta=\cos^{-1}\frac{R_a}{Z_a}$$

β is also the angle between the voltage I_aZ_a and the current I_a, where I_a is the armature current for any value of load

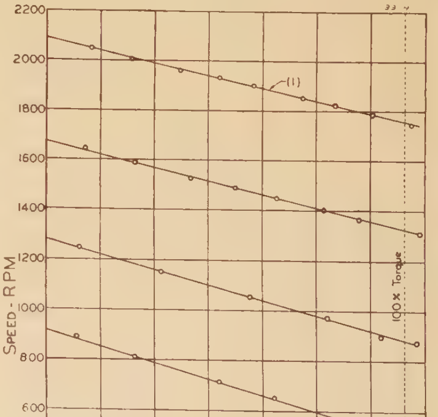

Figure 4. Speed-torque curves of the motor for different values of armature voltage

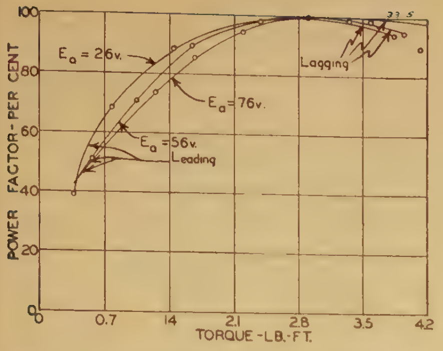

Figure 5. Power-factor curves for different speed settings

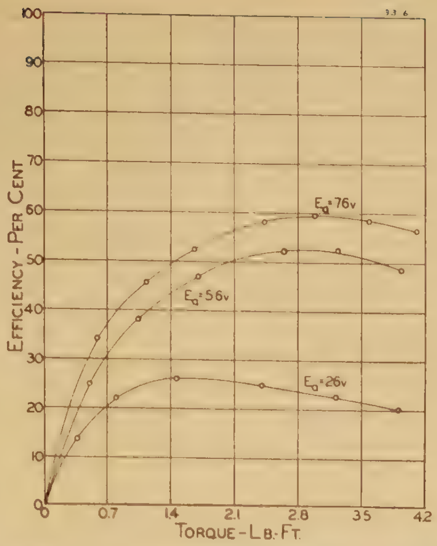

Figure 6. Efficiencies for different speed adjustments

The current I_{ab} acting on the flux ϕ produces torque. If the motor is allowed to rotate, a counterelectromotive force similar to the back electromotive force in a direct-current motor will be generated. This generated voltage E_g will have the same time phase as the flux ϕ, since it is proportional to the product of the flux and speed. Therefore the generated voltage vector $\boldsymbol{E}_g$ is parallel to the flux vector ϕ, and the magnitude of $\boldsymbol{E}_g$ is proportional to the speed of the motor if the flux is constant. The sum of the voltages in the armature circuit must equal the applied voltage, or expressed as an equation

$$I_aZ_a+E_g=E_a$$

In this relationship the voltage E_g has a fixed direction but can change in magnitude as explained above; however the voltage E_a is constant in both direction and magnitude as assumed. Therefore the term $\boldsymbol{I}_a\boldsymbol{Z}_a$ has a variable direction and magnitude such that the above equation is satisfied.

For an increase in speed, the magnitude of the vector $\boldsymbol{E}_g$ in Figure 3 increases along the dotted line *fd*—this line is parallel to the flux vector φ. The terminus of the vector $\boldsymbol{I}_a\boldsymbol{Z}_a$ must always lie along the dotted line *fd* in order that the vector sum of $\boldsymbol{I}_a\boldsymbol{Z}_a$ and $\boldsymbol{E}_g$ be equal to the vector $\boldsymbol{E}_a$. Thus the extremity of the vector $\boldsymbol{I}_a\boldsymbol{Z}_a$ has the straight line locus *fd*. Since the changes in the brush resistances are small over the operating range of the motor, the armature impedance Z_a is essentially constant. The armature current I_a is proportional to $\boldsymbol{I}_a\boldsymbol{Z}_a$; therefore, the terminus of the vector $\boldsymbol{I}_a$ also follows a straight-line locus in the same manner as the vector $\boldsymbol{I}_a\boldsymbol{Z}_a$. The locus of $\boldsymbol{I}_a$, shown as the dotted line *gh* in Figure 3, is displaced from the line *df* by the impedance angle β. The vector $\boldsymbol{I}_a\boldsymbol{Z}_a$ must lead the armature current vector by the impedance angle β.

The power developed is equal to the projection of E_g on I_a multiplied by I_a, and the copper loss in the armature circuit is equal to the projection of $\boldsymbol{I}_a\boldsymbol{Z}_a$ on I_a multiplied by I_a. At no load the developed power is small, therefore the current vector $\boldsymbol{I}_a$ is approximately perpendicular to the vector $\boldsymbol{E}_g$. For the condition of no load the armature current I_a as shown by the vector diagram is leading the applied voltage E_a. When the machine is loaded, there is a slight reduction in speed and consequently a reduction in the magnitude of the vector $\boldsymbol{E}_g$. This change in $\boldsymbol{E}_g$ causes a change in the magnitude and direction of the vector $\boldsymbol{I}_a\boldsymbol{Z}_a$—such that its extremity moves along its locus *df*. This clockwise shift of the vector $\boldsymbol{I}_a\boldsymbol{Z}_a$ is associated with an equivalent shift in the current vector I_a. The locus of the terminus of the vector $\boldsymbol{I}_a$ is *gh*. Thus an increase in load is associated with a reduction of speed. The torque supplied to the shaft is represented by the projection of the current vector $\boldsymbol{I}_a$ on the flux vector ϕ (Figure 3). Expressed as an equation

$$T=K\phi I_a\cos(\alpha+\theta)$$

where θ is the power-factor angle of the armature circuit. This angle θ is considered plus for leading power factors and minus for lagging power factors. The factor K is a constant and α is the angle between the flux vector ϕ and the voltage vector $\boldsymbol{E}_a$.

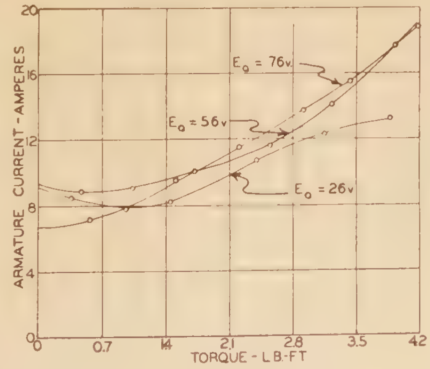

Figure 7. Armature-current curves for different speed adjustments

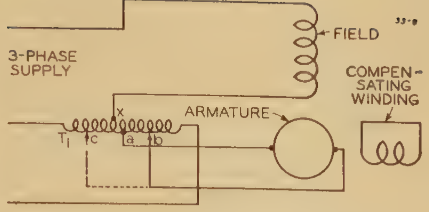

Figure 8. Circuit for reversing drive

Performance

Experiments conducted on a motor supplied as shown in Figure 2 reveal that the characteristics predicted from the vector diagram of Figure 3 check the performance characteristics of the motor. The speed-torque characteristics of this motor with different values of impressed armature voltage are shown in Figure 4. These speed-torque curves are straight lines and are parallel. For the high-speed setting the speed regulation of the motor is approximately nine per cent. These speed-torque curves indicate that the motor has an extremely high starting torque. Selection of speed can be accomplished by adjusting the armature voltage E_a. The power factor of the armature circuit for different speed settings is shown in Figure 5. The corresponding efficiency and armature current curves are shown in Figure 6 and Figure 7 respectively.

Since one of the three-phase lines to the motor carries only field current, while each of the other two lines carries the transformed armature current and a portion of the field current, the three line currents supplying this system must be unbalanced. The extent of unbalance for approximate equal intervals of load from no load to full load for the performance curve 1 of Figure 4 is indicated in Table I.

Additional Control Features

The motor described here can be easily adapted for reversing operation by the use of the circuit shown in Figure 8. In this circuit one side of the armature is connected to the mid-tap of the transformer. The other side of the armature can be connected to either side of the mid-tap for operation. When connected on the right (Figure 8) of the mid-tap, the direction of rotation of the motor will be reversed with respect to its direction of rotation when connected to the left. Thus reversing is easily obtained.

Other types of transformer connections can be used to supply this motor with the same terminal voltages as those discussed here. Such connections naturally provide the motor with the same operating characteristics. Field control can be added in these circuits, but it usually results in poorer motor characteristics. A slight amount of saturation in the field provides fair speed regulation in the motor. Weakening the field will cause an increase in speed, but it will also cause an increase in the speed regulation.

If the sliding contact is moved rapidly toward the other armature supply tap on the adjustable transformer, there will be a rapid reduction of the armature voltage E_a. This causes a reversal of the power component of the armature current. The machine will act as a generator, and high braking action will result. Thus the same control apparatus used for starting and controlling the speed can be used for braking.

Conclusions

1. The theory of a new type of adjustable-speed drive for use on alternating currents is presented here.

2. The theory is verified through experiments conducted on a two-horsepower motor and its controller. Performance of the system was found to be meritorious.

3. The system provides speed adjustment for either direction of rotation.

4. The speed range is from 0 to 100 per cent of the rated armature speed.

5. By proper adjustment the power factor of the motor is leading or close to unity from no load to full load.

Table I

Field Current Amperes	Line Currents Supplying the Armature Amperes	
I_1	I_2	I_3
9.5	12.2	6.1
9.5	13.0	6.0
9.5	15.1	7.1
9.5	16.4	8.0
9.6	17.4	8.2
9.6	19.3	9.7
9.6	22.9	13.5

6. The controller consists of an adjustable autotransformer and can be mounted independently of the motor itself.

7. High starting torques are available for accelerating heavy loads or loads with large inertias.

8. No energy is lost in starting resistors in this type of control. The system has smooth accelerating characteristics from standstill to maximum speed.

9. Since the armature currents on starting are supplied at low voltage, the input currents to the system are small, thereby eliminating line-voltage disturbances normally associated with motor starting.

10. Rapid acceleration and deceleration are provided in the control circuit.

11. Efficiency of the system is comparatively high.

12. Simplicity of the control circuit and its manifold uses make the unit relatively inexpensive.

13. The unbalance of line currents makes its application to large motors undesirable. The unbalance produced by small motors of this type is not objectionable.

List of Symbols

E_a—The voltage applied to the armature.
E_f—The effective value of the voltage applied to the field winding of the motor.
E_f'—The effective value of the voltage induced in the field winding by the flux ϕ crossing the air gap.
E_g—The effective value of the generated voltage in the armature.
I_a—The effective value of the armature current expressed in amperes.
I_{ab}—The effective value of armature current expressed in amperes when the motor is at standstill.
I_f—Effective value of the field current.
K—A constant for any particular motor involving the total number of turns on the armature, the number of parallel paths, and the number of poles.
R_a—The equivalent resistance expressed in ohms of the armature and compensating winding.
R_f—Resistance of the field winding.
T—The developed torque of the motor expressed in pound-feet.
x—The point on the adjustable transformer at which the field is connected.
X_a—The equivalent reactance of the armature and compensating winding expressed in ohms.
X_f—The effective reactance of the field winding expressed in ohms caused by the leakage flux that does not cross the air gap.
Z_a—The equivalent impedance of the armature and compensating winding expressed in ohms.
α—The phase angle expressed in electrical degrees between the generated voltage and the applied armature voltage.
β—The angle having a cosine that is equal to the ratio of the armature resistance R_a to the armature impedance Z_a.
θ—The phase angle or power-factor angle in electrical degrees between the impressed armature voltage E_a and the armature current I_a.
ϕ—The total flux per pole crossing the air gap expressed in maxwells.

A New Generator Differential Relay

A. J. McCONNELL
ASSOCIATE AIEE

"RELAYS have been available for years capable of providing selective high-speed protection if—and it is a big IF—the intelligence transmitted to the relay closely approximates the truth." This statement, recently made by a protection engineer, is a true statement, the import of which is not always fully recognized.

Regarding current supply, there are many relay applications for which orthodox current transformers are quite adequate. On the other hand, applications such as differential relaying require careful consideration of the current-transformer characteristics. If a high-speed differential relay is desired, transient, as well as steady-state characteristics must be studied. The reason for this is the probability of false differential current flow occasioned by d-c saturation of a current transformer excited by an offset current.[1]

There has been a gradual improvement in the steady-state characteristics of current transformers. Also, the transient problem has been attacked by increasing the size of the core, by inserting air gaps in the magnetic circuit,[2] or by eliminating the iron entirely.[3] By and large, however, the major portion of differential-relay development has been concerned solely with the design of relays capable of overcoming the inherent deficiencies of standard current transformers. There is no better example, in support of this statement, than the protection of generators against internal faults.

As of today, what better protection could be imagined than the very first instantaneous overcurrent relay, connected differentially, if the currents supplied to the relay were true representations of the actual primary currents? Actually, however, there have been at least five generator protective-relay developments, all concerned with the inadequacy of the current supply. Briefly, the chronology has been somewhat as follows:

(*a*). Time-overcurrent relay connected differentially.[4,5] False current-transformer output requires high minimum operating current, or long operating time, or both, in order to prevent improper relay operation.

(*b*). Percentage- or ratio-differential relay (relatively slow speed).[6] Such a relay is provided with a restraining force dependent upon the sum of the currents entering and leaving the zone of protection. During a fault external to the protected zone, it operates only if one current is a certain percentage above the other (slope). Figure 1, curve *A*, shows the operating characteristic of one such relay.

(*c*). High-speed percentage- or ratio-differential relay. Such relays have had only limited success because of the high false differential current possible, as above noted, during transient conditions.

(*d*). Harmonic-current-restrained differential relay.[7] Although transformer and bus relays of this type have been successfully applied, the harmonic-current-restraint principle has never been extended to generator protection because of design limitations attending the requirements of high sensitivity and ability to withstand heavy fault currents.

(*e*). Relay utilizing both (1) variable ratio (variable slope) and (2) the out-of-phase relationship between the differential and restraining currents during heavy through faults[8] (Figure 1, curve *B*) shows a variable-slope characteristic with all currents in-phase.

(*f*). A new differential relay, described herein, the restraining force of which is obtained by means of a principle heretofore unused. It restrains in response to the product of the currents entering and leaving the protected zone, and, consequently the new principle is called "product restraint." In addition, the relay has a slope which is variable and which increases very rapidly above approximately twice normal current (Figure 1, curve *C*).

Limitations of Variable Slope With Summation Restraint

In order to appreciate fully the principle, product restraint, it is necessary first to consider the limitation of variable ratio or slope as applied to a relay using the orthodox summation restraint.

Variable ratio or slope, as the names imply, signifies that the slope of the through-fault operating characteristic continuously changes, as shown by curve *B* of Figure 1. In order to be really effective, however, the slope should increase rapidly above maximum full-load current of the machine, perhaps as indicated by curve *C* of Figure 1. To obtain such a relay characteristic, it is apparent that, as the current increases, the operating force must become deficient relative to the restraining force. However, assuming summation restraint, a relay with this characteristic could fail to operate under certain internal fault conditions. For example, if the internal fault is fed only by the protected generator (that is no external source), the restraining and the differential currents are identical. Figure 2 shows curves for the operating and restraining forces of a relay with a variable slope, summation restraint, and identical restraining and operating currents. For currents above the intersection of the curves, the relay will fail to operate, because the restraining force is in excess of the operating force.

For simplicity, it was considered that the curves of Figure 2 were based upon an internal generator fault without an external source of feed. Actually, the danger of failure to operate may be greater when there is an external source. With equal currents fed from the protected generator and an external source, the restraining force of a summation restraint relay is theoretically zero. However, the current from the external source may be much greater than the current supplied from the protected generator (for example: there may be several generators on the same bus). Consequently, the restraining force may be much greater when there is an external source than when there is not, and there may be a greater possibility of nonoperation. For example, assume a generator bus with four identical generators connected to it (and no other source of power). The total fault current during a bus fault is four times the current supplied by each generator. If the fault is on the leads of a generator (within its differentially protected zone), the differential current is four times and the net restraining current is twice the respective currents for a fault on the generator leads

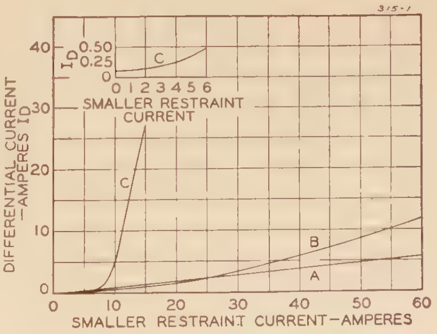

Figure 1. Through-fault operating characteristics of three kinds of differential relays

Restraining and differential currents in phase

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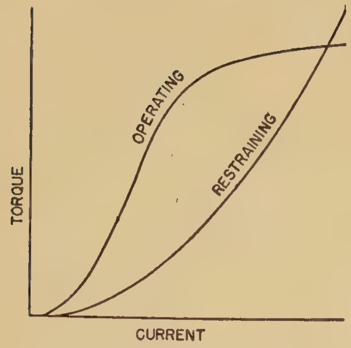

Figure 2. Torque curves of saturated operating element and unsaturated restraining element (summation restraint illustrating possible failure to operate on internal fault)

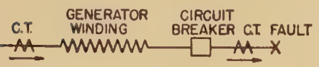

Figure 3. Directions of currents in current transformers during external fault

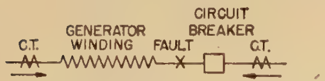

Figure 4. Directions of currents in current transformers during internal fault

with no external source. Relative to the current at the intersection of the curves of Figure 2, doubling the restraining current increases the restraining force to four times, whereas increasing the operating (differential) current to four times increases the operating force by much less than four times. Hence, the increase in restraining force relative to operating force increases the possibility of failure to operate when there is an external source.

Product Restraint

In order to retain the desired through-fault characteristic, curve *C* of Figure 1, and yet provide positive operating margin during an internal fault, it is necessary that the restraining force be greatly reduced, or, preferably, entirely eliminated during an internal fault. The latter result can be accomplished by restraining in response to the product of the currents entering and leaving the protected zone, rather than to their sum. Thus, during an internal fault fed only by the protected generator, the restraining force is zero, since one of the two terms of the product is zero.

For a through fault, the relative polarity of the restraining winding is such that the force is in the contact-opening direction. For an internal fault, assuming that the generator is connected to another source of power, the relative direction of the fault currents is reversed so that the so-called restraining windings will actually produce a contact-closing force. Thus, the windings which provide the restraining force during a through fault, produce, in so far as fault current is concerned, either an operating force or no force during an internal fault.

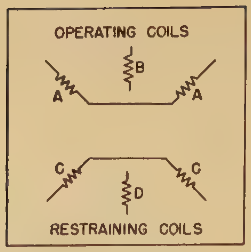

Figure 5. Coil locations in product-restraint differential-relay element

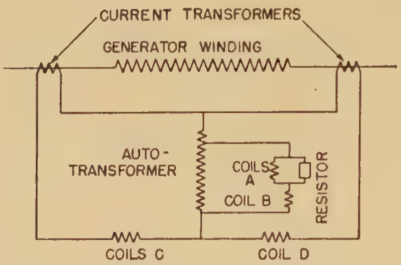

Figure 6. Schematic diagram showing relay and auxiliaries for one phase

A somewhat different conception of the operation of this relay results if the relay is considered to be primarily a directional relay. Figure 3 shows that, for an external fault, the current flow is in the same direction in the current transformers on opposite sides of the generator winding. Figure 4 shows that the short-circuit currents in the two current transformers are in opposite directions during an internal fault.

If Figure 4 were true under all conditions, a current polarized directional relay, one winding connected to each current transformer, would provide adequate protection against internal faults. However, there are two conditions during which Figure 4 is in error. First, there may be no external source, in which case the directional relay would fail to operate. Second, load current appears to the directional relay to be an external fault of relatively low current magnitude as in Figure 3. If the internal fault current is low, the load current may outweigh the fault current, thereby preventing the current reversal required for relay operation.

In order to provide sensitive operation with load current flowing and to permit operation when there is no external source of power, a differentially connected overcurrent relay can be added to the directional relay, both relays acting upon a single shaft. Although it is necessary that the overcurrent element be sensitive in order that low values of short-circuit current may overcome the effect of load current, it is not necessary that the overcurrent element torque ever be greatly in excess of the restraining torque of the directional element under maximum load current conditions. This fact permits sharp saturation of the differential current circuit, thereby causing the relay to be stable during external faults, even with the large false differential currents which may be present at the higher current levels.

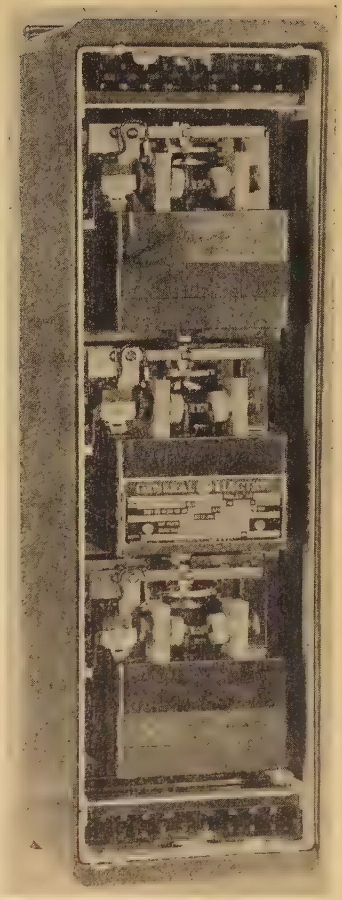

Figure 7. Three-phase product-restraint differential relay

Description of Relay

The relay unit using the product-restraint principle is of the eight-pole, induction cylinder construction previously described before the Institute.[9] The available poles permit both the differential-operating and the product-restraining functions to be accomplished in a single unit. Although a single relay unit is described, it should be understood that three such units are required for a three-phase circuit.

Figure 5 shows the locations of the various coils on the eight-pole structure. The upper three coils, denoted by *A* and

B, are the differential-operating coils, while the lower three coils, *C* and *D* provide the product restraint.

Figure 6 shows schematically the connections of the various coils to the current transformers spanning the protected zone.

Figure 6 indicates that the differential current is not supplied directly to the upper coils of Figure 5, but, rather, indirectly through an autotransformer. The autotransformer, which saturates at a relatively low current, provides the greatly increasing slope characteristic as the through-fault current increases. It also permits the use of relatively large wire in the differential circuit, thereby facilitating testing at the higher currents without the overheating usually encountered in testing generator differential relays.

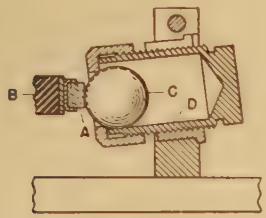

Figure 8. Cross-sectional view of relay backstop

It will be noticed that a resistor is used in the circuit of coils *A*. This resistor is for the purpose of shifting the phase of the current in the outer poles to obtain a torque-producing component. The necessary phase shift of the product-restraining coils is obtained easily in the relay unit itself by means of a flux-shifting copper tube.

Figure 7 shows a three-phase relay, all auxiliaries of which are mounted behind the relay units. Those familiar with this construction will recognize the usual bounce-suppressing contact on the left-hand side of the upper structure of each relay unit. Because of constructional similarity, there appears to be a contact on the right-hand side also. However, the tube or "barrel" on the right-hand side is a new type of backstop construction which prevents closing the tripping contacts upon the release of a through-fault restraining force. This backstop has another desirable quality in that it acts as a shock-suppressing device. The relay can be set as low as 0.1 ampere at no load, as indicated by the insert of Figure 1. However, for practical purposes 0.2 ampere is sufficiently low, and a relay calibrated at this value will be safe against false operation, even under unusual shock conditions.

Figure 8 shows a cross-sectional view of the backstop. Felt pad *A*, attached to the moving contact arm *B*, normally rests against stainless-steel ball *C*, which in turn is free to roll in race *D*. The race is inclined sufficiently to cause the ball to retain the contact arm in the position shown under normal conditions with full load current flowing. Upon the occurrence of an external fault, the restraining torque causes the moving contact arm to force the ball to the right, opening the contact gap and thereby effectively preventing the stored energy from causing contact closure upon the release of that energy when the external fault is cleared. Regarding the shock-resistant quality of the backstop, consider first the action of a solid backstop following a blow from the right. The energy of the impact would be transmitted, with practically no attenuation, to the moving contact arm, causing it to move to the left, or contact-closing direction. With the backstop of Figure 8, a blow from the right causes race *D* to move sharply to the left. However, since the ball is free to move relative to the race, the inertia of the ball causes it, together with the moving contact, to remain substantially motionless in space.

Relay Characteristics

The typical through-fault operating characteristic of this relay is that previously referred to as the "desired characteristic," curve *C* of Figure 1. The insert of Figure 1 shows the same curve at low current levels and can be used for internal faults, the abscissas then indicating the load currents. Any further attempt to depict the internal fault characteristic would be quite meaningless, since, as previously explained, a flow of short-circuit current from an external source causes an operating force to be produced by the so-called restraining windings and thereby, in effect, reduces the minimum operating current of the relay.

The relay is of the high-speed type as indicated by the time-current curve, Figure 9.

The burden of the restraining coils is very low, approximately 0.08 ohm total. The burden of the operating-coil circuit is a variable, reaching a maximum of 65 ohms. This seems high but is actually comparable with, if not less than, the burdens of other generator differential-relay operating circuits.

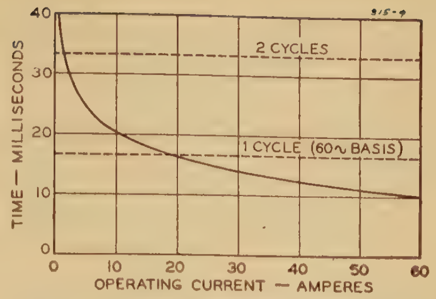

Figure 9. Typical time-current curve of relay with restraint corresponding to full load

Conclusion

A generator differential relay has been described which approaches the ideal of fast sensitive protection equivalent to that attainable with perfect current transformers. Because the product-restraint principle permits a variable slope without its usually attendant "blind-spot" danger, the relay can be applied with a minimum of effort.

Thus far, the generator differential relay is the only relay to which the product-restraint principle has been applied. This development was completed before December 7, 1941, but further extension of product restraint such as to transformer, pilot wire, or bus differential relays, must await normal times.

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An Instrument for the Determination of Contact Making and Breaking Time

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Synopsis: This paper describes an electronic instrument for the measurement of the arcing time of a contact closing or opening a circuit. This measurement is accomplished by permitting a current of constant value to flow into a capacitor during the time that an arc exists across the contacts under observation; the charge accumulated on the capacitor is therefore a measure of the arcing time. The instrument also contains electronic means of measuring the charge on the capacitor, by determining the voltage existing across it at the end of the measurement. The circuit serving this purpose is a Wheatstone bridge with a tube in one arm, to whose grid the capacitor is connected, and a microammeter in the diagonal which indicates directly the amount of unbalance produced by the capacitor voltage.

The application of the instrument to the investigation of a three-pole contactor is described.

FOR the investigation of contact-making devices such as contactors, relays, and snap switches under various load conditions, a device for the quick determination of the operating time of a contact seemed desirable. As many investigators in the field have found that the making of a circuit often represents a harder service for the contact-making device, because of contact bounce, than the breaking of the circuit, the instrument described in the following paragraphs was originally designed to permit the quick measurement of the bouncing time of a contact, without the need of employing an oscillograph.

The underlying principle of the instrument is very simple; a current of constant and predetermined value is made to flow into a capacitor during the time that the contact is being made or broken, that is, during the time that an arc exists across the contacts. The charge accumulated on the capacitor and with it the voltage appearing across it are therefore proportional to the time of arcing, since the charge is the time integral of the current flowing into the capacitor.

Obviously, mechanical relays cannot be used to connect and disconnect the measuring capacitor to and from a suitable charging circuit during the arcing time of the contact-making device under observation, since the error introduced by these relays would be of the same order of magnitude as the time to be measured. A vacuum tube, on the other hand, is a practically inertialess relay and thus offers a convenient solution to this problem. Furthermore, if a capacitor and a tetrode or pentode, properly biased, are placed in series across a fixed d-c voltage, the current which will flow in this series combination, that is, the charging current of the capacitor, will remain practically constant, as a result of the flat plate characteristics of these types of tubes, until the voltage across the tube has been reduced to a relatively low value. A nearly linear relation between time and accumulated charge is thus obtained.

Figure 1 shows that part of the circuit which accomplishes the charging of the capacitor. It is seen that the capacitor is placed in series with a pentode across a suitable d-c voltage. The capacitor is by-passed by the meter M_1 and the normally closed switch S_2, which is opened just before a measurement is taken and is kept open only until the measurement is completed. The grid bias of the pentode consists of voltage drops caused by currents flowing in the resistors R_1 and R_3, as will be explained in more detail later. During the adjustment of the charging current to a suitable value, as well as during the actual arcing time, no current flows through R_3, and the bias voltage consists only of the voltage drop e_o across R_1. This voltage is of constant value and such that it permits plate current to flow, the amount being adjustable to any desired value by varying the screen grid voltage obtained from the potential divider P_1. If switch S_2 were opened at this moment the plate current, which up to this instant was indicated by the meter M_1, would flow through the capacitor, charging it linearly with time. Switch S_2 is not opened, however, until additional bias, furnished by a current flowing through resistor R_3, has biased this tube beyond cutoff.

It now becomes evident that means must be provided to maintain a current flow through R_3 sufficient to bias the charging tube to cutoff, except during the arcing time of the contacts under observation, at which time the current flow through R_3 should be reduced to zero, so that the previously adjusted charging current will flow into the capacitor, as already explained. Furthermore, the instrument should indicate directly the charge accumulated on the capacitor during this period. The circuits accomplishing this are shown in Figure 2.

A conventional rectifier system furnishes the necessary d-c voltage; two voltage-regulator tubes in series keep the supply voltage constant. Previous to an actual measurement switch S_1 is opened, which prevents any possible current flow through R_3 and thus permits the adjustment of the plate current, as outlined above. S_2 is kept closed, of course, during this operation. Consider now the two control tubes V_1 and V_2. Resistance R_4 is common to both plate circuits. If either one of these tubes is made sufficiently conducting, the current flow in this resistance will make point H negative with respect to point F, or, in other words, the voltage across the control tubes will be less than the voltage across R_2. With switch S_1 closed, current will therefore

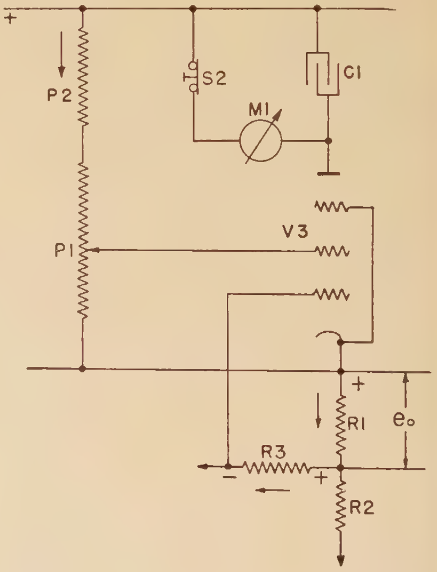

Figure 1. Detail of capacitor charging circuit

V_3—6C6 tube
C_1—0.1-, 0.25-, 0.5-, 1.0-microfarad mica capacitor (depending upon desired range)
R_1—250-ohm 10-watt wire-wound resistor
R_2—5,000-ohm 10-watt wire-wound resistor
R_3—150,000-ohm 1-watt carbon resistor
P_1—5,000-ohm wire-wound potential divider
P_2—1,000-ohm wire-wound potential divider
M_1—0-1-milliampere d-c meter

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flow from point F through R_3 and the diode to point H. This provides the additional bias voltage needed to cut off the current in the charging tube. If the current flow through R_4 is reduced to such a value that point H becomes positive with respect to point F, the current through R_3 ceases, since the diode prevents a reversal of current, and the charging tube passes the previously adjusted current.

The contact to be investigated in series with a suitable load is connected across a d-c source with a polarity as shown in Figure 2. A resistor R_{10}, acting similarly to a shunt, is included in the circuit. Its resistance should be such as to produce a voltage drop of at least 0.25 volt with the lowest value of load current. This voltage drop furnishes a negative bias to one of the control tubes, whereas the second control tube receives a negative grid voltage when a voltage exists across the contacts. It is evident, therefore, that before the contact closes, V_2 will be biased negatively, whereas V_1 will have zero grid voltage (since no current flows in the shunt); V_1 will consequently be conducting, its plate current passing through R_4. After the contact is established firmly, however, V_1 will become nonconducting as a result of the bias produced in the shunt, whereas V_2 will now become conducting. The inclusion of a dry cell in the grid circuit of this tube serves to overcome any possible effects caused by contact resistance. It is seen that during the period of arcing both control tubes will be biased negatively, V_1 by the voltage drop across the shunt and V_2 by the arc voltage existing across the contacts. Current flow through R_4 is therefore reduced or ceases entirely during the arcing period, which in turn permits current flow in the charging tube, as explained above. Since S_2 is opened just before a measurement is taken, a charge will accumulate on the capacitor.

The remaining circuit element serves to measure the voltage across the capacitor. Any device for this purpose—unless a ballistic method were to be considered—must draw no appreciable current for the measurement. An electrostatic voltmeter would be the ideal instrument, of course, but for the relatively low voltage to be measured this type of instrument is very delicate. Special tubes, called electrometer tubes, such as General Electric *FP*-54 are available, the grid current of which is about 10^{-15} amperes, with a corresponding input resistance of about 10^{16} ohms. Since a charged capacitor will lose its voltage as a result of its own leakage resistance, however, it is obvious that not much is gained by employing an instrument with an input resistance two or three powers of ten higher than the leakage resistance of the capacitor and its associated circuits.

Of the common radio tubes, the type 38 has a grid current of about 10^{-9} amperes when operated with a filament voltage of four volts and with about 30 volts on the screen and plate. This tube is used in the instrument forming one arm of a Wheatstone bridge. The rate of change of capacitor voltage, caused by the combined action of leakage and grid current, is so low in the instrument that the drift of the meter does not exceed one scale division in three or four seconds, so that ample time for taking a reading is available.

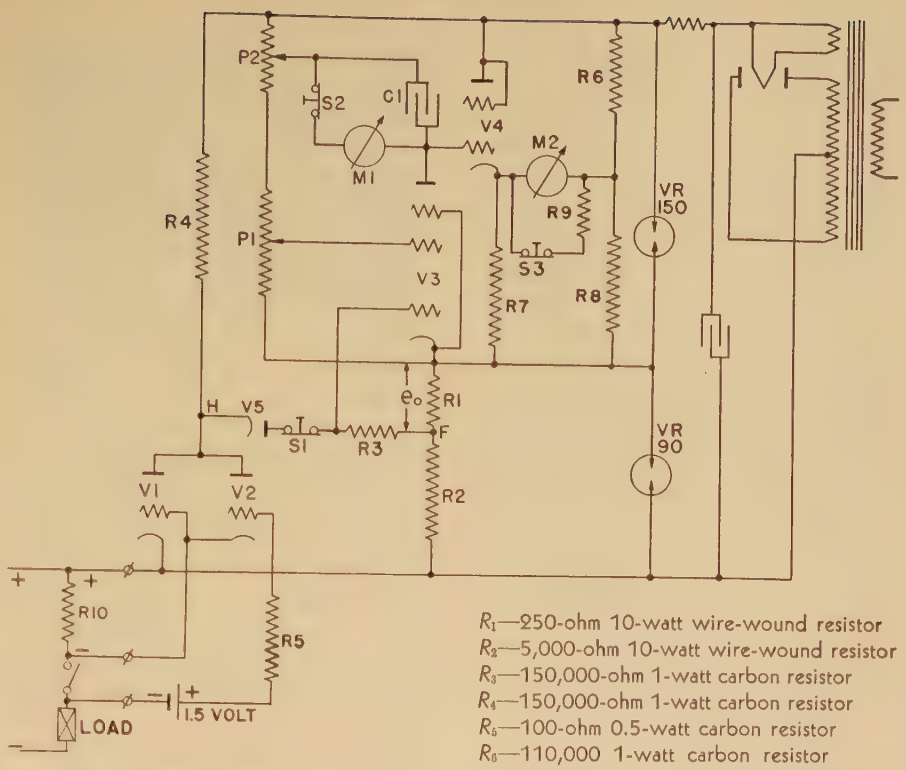

Figure 2. Circuit diagram of instrument

V_1—*6F5* tube
V_2—*6F5* tube
V_3—*6C6* tube
V_4—*38* tube
V_5—*6H6* tube
C_1—0.1-, 0.25-, 0.5-, 1.0-microfarad mica capacitor (depending upon desired range)

R_1—250-ohm 10-watt wire-wound resistor
R_2—5,000-ohm 10-watt wire-wound resistor
R_3—150,000-ohm 1-watt carbon resistor
R_4—150,000-ohm 1-watt carbon resistor
R_5—100-ohm 0.5-watt carbon resistor
R_6—110,000 1-watt carbon resistor
R_7—225,000 1-watt carbon resistor
R_8—One-megohm 1-watt carbon resistor
R_9—100-ohm 0.5-watt carbon resistor
R_{10}—Shunt (see text)
P_1—5,000-ohm wire-wound potential divider
P_2—1,000-ohm wire-wound potential divider
M_1—0–1-milliampere d-c meter
M_2—0–200-microampere d-c meter

Before a measurement is made, the bridge is balanced (that is, meter M_2 is brought to zero) by adjustment of potential divider P_2, which furnishes grid voltage for V_4. Switch S_2 is closed during this adjustment, of course. The meter M_2 can once and for all be calibrated in terms of voltage across the capacitor by simply applying known voltages across it, keeping S_2 open during this calibration. With the capacity of the capacitor known, the charging current adjusted to a known value, and the meter M_2 calibrated in terms of capacitor voltage, all factors for the calculation of the duration of current flow are therefore known. Since the measuring circuit is highly degenerative, because of cathode resistor R_7, the indications of the meter M_2 are practically linear with respect to the capacitor voltage. As a matter of interest, it may be stated that a capacitor voltage of approximately 20 volts produces full-scale deflection on the 200-microampere meter M_2.

Since the action of the instrument depends on the negative bias applied to the two control tubes, the contact under observation can be tested only with direct current. At first sight this might be considered as a serious limitation of the instrument. The addition of relatively simple rectifying circuits, however, would make the use of the instrument on a-c circuits possible. This was not deemed necessary, or even desirable, for the following reason: the performance of a contact in an a-c circuit will naturally depend on the exact instant of the a-c cycle when the contact opens or closes. Thus, a number of readings taken on the same contact will vary widely, unless care is taken to synchronize the contacts to a particular instant of the cycle. If the test is made with direct current, the latter can be adjusted to any value, thus simulating any desired "instantaneous" value of alternating current.

Figure 3 is a picture of the instrument. A tap switch not shown on the diagram is provided to insert various values of capacitance, thus changing the time range of the instrument. These values are 1.0, 0.5, 0.25, and 0.1 microfarad, and, with the charging current adjusted to one milliampere, full-scale deflection of the meter M_2 represents 20, 10, 5, and 2 milliseconds respectively. These values can be increased or decreased, naturally, by further changes of capacitance or adjustment of the charging current to a different value.

The instrument has been used successfully in the investigation of relays and contactors. The effect of changes made in the mechanical structure of the contactor and the effect of changes in the load can be studied quickly without having to wait for the development and measurement of films taken with an oscillograph. The instrument can also be used for purposes other than determining the arcing time of a contact. Thus, the operating time of relays or solenoids can be determined by inclusion of contacts at the beginning and the end of the travel, furnishing suitable voltages to the terminals of the instrument in place of those produced by the shunt or the arc. The measurement of the L/R ratio of an inductive load can also be carried out with the aid of a relatively simple external circuit.

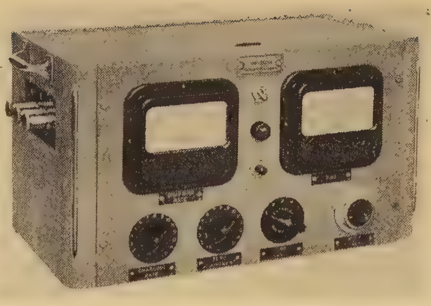

Figure 3. Electronic time gauge

A specific example of the use of this instrument and the benefits derived therefrom is the recent investigation of the closing bounce of a 1,200-ampere three-pole a-c contactor. Observations of the bounce were made as each set of contacts closed a 100-ampere 115-volt d-c noninductive circuit. The nominal voltage of the operating coil was 208 volts, but measurements were included at 195 and 220 volts, so that the effect of voltage variations might be ascertained.

Duration of bounce of the set of contacts behaving most poorly under the original adjustment of the contactor was 4.8, 5.6, and 6.4 milliseconds with coil voltages of 195, 208, and 220 volts respectively.

By increasing the initial contact pressures from the original value to a somewhat higher one, the respective bouncing times were reduced to 2.8, 3.4, and 4.5 milliseconds.

After increasing the wear allowance of the two outside sets of contacts slightly, setting the middle movable contact ahead of the others by one-eighth inch, and closing an orifice in the dashpot which allowed quick motion at the start of the armature stroke, the durations of bounce were 1.2, 1.4, and 1.7 milliseconds, respectively.

And, finally, after decreasing the clearance between the piston and cylinder of the dashpot assembly, the bounce durations were found to be 0.3, 0.3, and 0.5 millisecond.

Within a few hours time, therefore, it was possible to observe the effect of several variables upon the action of the contactor and to reduce the duration of closing bounce to about seven per cent of its original value. To accomplish this by means of an oscillograph would obviously have required a considerably greater expenditure of time and effort.

In another case, it became necessary to reduce the time required for a small relay to interrupt a circuit. Observations were made thereupon of the duration of the opening arc both with and without blowout assemblies. The arcing time without a blowout, for example, was found to be 14 milliseconds, with a coil-type blowout 7.5 milliseconds, with a tool steel magnet blowout 2.6 milliseconds, and with an Alnico magnet 2.2 milliseconds. The effectiveness of numerous magnet designs was thus readily observed, and an educated selection made of the most suitable one for the purpose.

Correlation of System Overvoltages and System-Grounding Impedance

AIEE COMMITTEE ON PROTECTIVE DEVICES
Working Group on Correlation of System-Grounding Impedance

Synopsis: This report summarizes available data and engineering opinions on the subject of power-system overvoltages, particularly as to their correlation with system-grounding impedance. It is hoped that the report will be useful in connection with overvoltage problems for all types of power-system apparatus.

THE characteristics of power-system connections with respect to ground have long been recognized as having an important bearing on many phases of system operation, including overvoltages between line conductors and ground.[1,2] As early systems (which were of the ungrounded type) became extended, operating experience became unsatisfactory because of the occurrence of multiple faults at the same or different locations resulting from an initial ground fault. Since troubles of this nature were frequently thought to be associated with arcing faults, such faults became broadly classified as "arcing grounds." Difficulties thought to be attributable to this type of fault were one of the main factors leading to the general practice of grounding power systems. However, the correlation of system overvoltages with the various forms of grounding and the values of grounding impedances was so complicated that many years elapsed before much progress was achieved, and even now many conditions are incompletely classified.

Causes of System Overvoltage

In a study of system overvoltage phenomena and their correlation with system-grounding impedance, it is convenient to list the causes. Causes of system overvoltage (other than lightning) may be classified as follows:

1. Conditions resulting in sustained overvoltage:
(*a*). Increase of operating voltage level.
(*b*). Generator overspeed.
(*c*). Self excitation.

2. Circuit unbalances:
(*a*). Open phase or phases.
(*b*). Unequal pole opening of breakers.
(*c*). Fuse blowing.

3. Faults:
(*a*). Solid.
(*b*). Resistive.
(*c*). Arcing with forcing of current zero.
(*d*). Arcing with restriking.
(*e*). Distortion from saliency effects.

4. Switching operations:
(*a*). Simple.
(*b*). Arcing with forcing of current zero.
(*c*). Arcing with restriking.
(*d*). Neutral inversion.

5. Extraneous influence from other sources:
(*a*). Conduction, metallic cross or ground potential, from other power circuits.
(*b*). Induction, magnetic or electric, from other power circuits.
(*c*). Electrostatic charging of ungrounded systems by wind-borne particles, such as sand or dust.

Frequently overvoltages result from combinations of two or more of the foregoing factors. In order to define the various causes of system overvoltages, a brief discussion of each type of phenomenon is given.

CONDITIONS RESULTING IN SUSTAINED OVERVOLTAGE

On ordinary power systems overvoltages at fundamental frequency may be produced by:

(*a*). Increase of operating voltage level.
(*b*). Generator overspeed from loss of load.
(*c*). Self excitation of machines.

Five per cent tolerance for emergency operation is the maximum overvoltage above rated circuit voltage recognized by the joint committee on preferred voltage ratings of the National Electrical Manufacturers' Association and the National Electric Light Association.[3] Application procedures for standard rated apparatus are based on this maximum circuit voltage. Sustained overvoltages generally are, under normal conditions, of little importance in regard to voltage stress on apparatus and functioning of protective equipment, but they may become important during or following an abnormal condition, such as produced by a lightning discharge or fault to ground.

It is believed that generator overspeeding and loss of load as single factors without faults or unbalances will seldom give rise to dangerous overvoltages. However, overspeeding may be a contributing factor, especially if it occurs during ground faults. The voltage amplitude is a function of governor characteristics and of means for controlling the excitation. With water-wheel generators without regulation of speed or excitation, the maximum voltage may be as high as 160 to 180 per cent of normal under full-load rejection; whereas with machines under control of modern governors and generator voltage regulators, the voltage will probably be limited to 120 to 140 per cent of normal.

Self-excitation is an infrequent source of overvoltage on systems. It can occur, however, under abnormal circuit conditions which leave relatively small synchronous or induction machines connected to circuits with relatively large capacitance, supplied from lines or capacitors. The possibility of setting up the conditions for self-excitation should be considered in the various stages in the disconnection of faulted circuits.

Sustained overvoltages under unbalanced load conditions may also be produced by saliency effects in machines.[4,5] Solid faults, other than balanced three-phase, sometimes cause the generation of dangerously high harmonic overvoltages in addition to increased fundamental-frequency voltages. These overvoltages are obtained when the system electrical constants and loads are of such proportions as to provide partial resonance to one of the lower harmonics. Magnitudes of overvoltage of this type depend not only on the type of unbalanced fault and the method of grounding but on other system constants as well. Such phenomena are most frequently encountered on systems with water-wheel generators without adequate damper windings, under conditions in which the machines are connected to unloaded or lightly loaded transmission lines with appreciable capacitance and subjected to unbalanced faults.

CIRCUIT UNBALANCES

On three-phase power systems the opening of one or more phase conductors will, under some circuit conditions, produce large overvoltages between line conductors and ground. These voltages are normally observed on the part of the

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Personnel of working group: W. W. Lewis, *chairman;* J. R. Eaton, R. D. Evans, P. A. Jeanne, J. W. Milnor, H. A. Peterson.

system which is partially separated from the main power source, under conditions with circuit constants such as to provide approximate resonance at fundamental frequency for some path between line conductors and ground. In general, these voltages are encountered on isolated-neutral systems or on systems having relatively high impedance between phase conductors or between phase conductors and ground. These circuit unbalances may arise from open-phase operations, such as occur as a result of conductor breaking, fuse blowing, or the opening of one or more breaker poles. Extreme conditions are produced with two synchronous systems which are out of step but are connected on one phase, the highest voltages being produced with both of the systems ungrounded and one of them subjected to a line-to-ground fault on either of the other two phases.

FAULTS

Faults cause the most frequently encountered and usually the most important power-system overvoltages. When a balanced three-phase system is subjected to a single or double line-to-ground fault, overvoltages may be produced on the unfaulted phase or phases. The amount of overvoltage may readily be calculated if the system constants, including nonvariable fault resistances, are known. If the fault takes place through an arc, the overvoltages may be increased by two phenomena:

1. The forcing of current zero.
2. Restriking.

Both of these phenomena are discussed at length subsequently.

SWITCHING OPERATIONS

Transients resulting in overvoltages of important magnitudes are produced by simple circuit changes involving a single transition from "make" to "break" or vice versa, as may occur on the energizing or de-energizing of a line section. Circuit changes may produce unbalances and thus result in overvoltages resulting from both unbalances and circuit changes. When circuit changes take place through an arcing path, there is also the possibility of increasing system overvoltages through the action of "forcing current zero" or of restriking as mentioned previously in connection with arcing faults. Since switching operations can take place in many ways in opening or closing the several poles of the circuit breaker, and since many transients may be produced for each of these steps, many combinations are possible. Switching operations are normally encountered in combination with other factors including faults and circuit unbalances, so that such transient overvoltages are usually the result of a combination of several factors.

EXTRANEOUS INFLUENCE

Occasionally a circuit is subjected to voltages which are impressed by conduction from metallic cross or ground-potential effects or by electric or magnetic induction from an adjacent power circuit, usually of higher voltage and capacity. Electrostatic charging of conductors during sand, dust, and sometimes snow storms, occurs in some regions. These effects are readily studied in a particular case if this cause of overvoltage is recognized.

Correlation of Overvoltages With System-Grounding Impedance

The various types of system overvoltages when appearing separately can be correlated with system-grounding impedance, except those that produce overvoltage under balanced conditions, such as emergency operating voltage, sudden loss of generator load, self-excitation of machines, and saliency effects (on line-to-line faults). However, in specific cases each of these types can be so correlated when the overvoltage occurs in combination with a fault to ground or other unbalances with respect to ground.

For correlation with system-grounding impedance, it is convenient to use a different classification from that just used for listing the causes of system overvoltage, basing the classification upon the type of transient phenomenon involved:

1. Simple circuit changes:
   (*a*). Ground faults.
   (*b*). Switching operations.
2. Complex circuit changes through arcing paths:
   (*a*). Forcing current zero.
   (*b*). Restriking arcs.

By a "simple circuit change" is meant a single circuit change that takes place with abrupt transition from "circuit open" to "circuit closed" condition or from "circuit closed" to "circuit open" condition, the opening taking place at a "normal current zero." By a "complex circuit change" is meant one that takes place through an arcing path which introduces additional circuit voltage transients either by

1. Forcing current zero.
2. Re-establishment of a conducting path as with intermittent arcs.

Complex circuit changes in general involve the superposition of a second transient before the effect of the first transient has disappeared.

SIMPLE CIRCUIT CHANGES

Calculation of Fundamental-Frequency Overvoltage

The simplest and most important, although not necessarily the most severe, types of system overvoltage produced on a power system occur as the result of the application of a single or double line-to-ground fault on a normal balanced three-phase power system. Upon such an occurrence, the sound phases may rise to fundamental-frequency overvoltage with respect to ground as determined by the sequence impedance constants of the system. In addition, there is a transient term which, in simple systems without loss, is equal to the difference between the fundamental voltages before and after the application of the fault. The fundamental-frequency components of overvoltage are of controlling importance in lightning-arrester applications, and the ratios of the fundamental-frequency voltages under fault condition to the normal voltages permit estimating the transient overvoltage magnitudes.

The fundamental-frequency overvoltages that appear on a balanced three-phase system upon the application of a single or double line-to-ground fault are readily calculated for the point of fault by well-known methods based on symmetrical components using the normal voltage before the fault and the sequence impedance constants of the system as viewed from the fault location, including X_0, X_1 and X_2, and the fault resistance or resistances.[6,8,9,11] Voltages at locations other than the point of fault will not as a rule appreciably exceed those at the point of fault, except possibly in case of a very long line, the far end of which has an isolated neutral.

It has been found possible to compare the grounding characteristics of systems by expressing overvoltages to ground in terms of normal voltage (usually on a line-to-neutral voltage base) and by expressing the zero-sequence resistance and reactance as ratios to the positive-sequence reactance.[6] When this procedure is adopted, it is possible to group systems which give the same ratio of overvoltage, although the characteristics of the systems may be radically different. In addition, it is sometimes desirable to give the ratio of the zero- to positive-sequence capacitive reactances. The effect of fault resistance is to increase the magni-

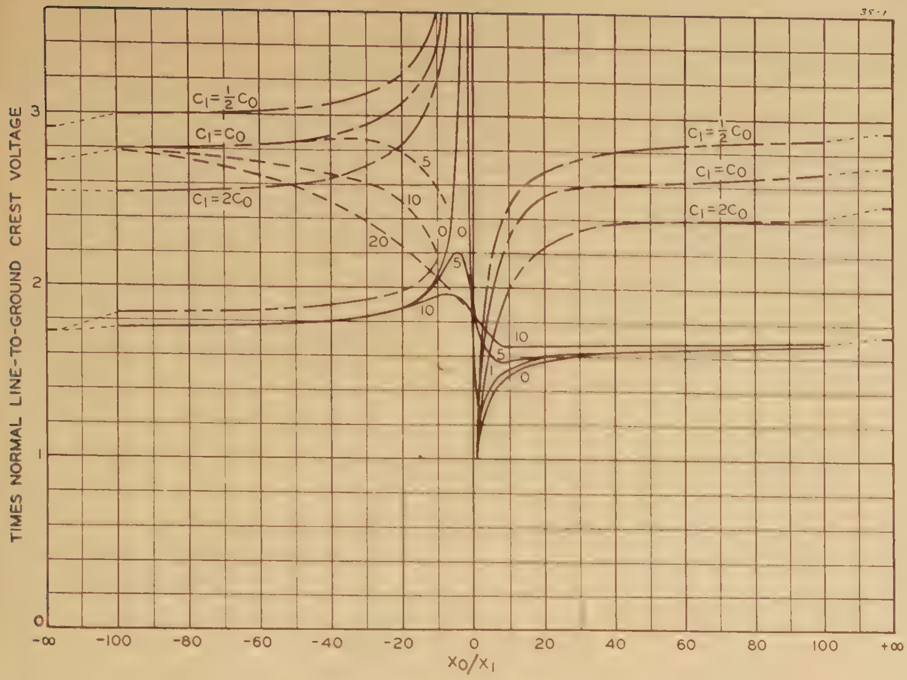

Figure 1. Transient and sustained fundamental-frequency voltages on unfaulted phases during single line-to-ground faults on a three-phase system

$Z_1=Z_2=R_1+jX_1=0+jX_1$
Z_1 = positive-sequence impedance
Z_2 = negative-sequence impedance
R_1 = positive-sequence resistance
X_1 = positive-sequence reactance (subtransient)
X_0 = zero-sequence reactance
C_1 = positive-sequence capacitance
C_0 = zero-sequence capacitance
R_f = fault resistance
All viewed from the fault location

Solid line curves—maximum sustained fundamental-frequency voltage to ground on unfaulted phases with solid single line-to-ground fault. No fault resistance. Numbers on curves are R_0/X_1 ratios

Dash-dot-dot-dash curve—maximum sustained fundamental-frequency voltage to ground with the value of fault resistance which gives the maximum voltage to ground for any value of R_0/X_1. Curve plotted only on negative side, as fault resistance is not as important on the positive side, and additional curves would lead to confusion

Dash-dot-dash curves—maximum transient voltage to ground on unfaulted phases following sudden application of a single line-to-ground fault. $R_0/X_1=0$

Dashed curves—maximum transient voltage to ground on unfaulted phases following sudden application of a single line-to-ground fault. $C_1=C_0$. Numbers on curves are ratios of R_0/X_1. The dash-dot-dash curve for $C_1=C_0$ is one of this family of curves for $R_0/X_1=0$

tude of overvoltage under some conditions and decrease it in others.

Correlation With Different Forms of System Grounding

The preceding discussion has shown the complexity of the problem of correlating overvoltages with system-grounding impedances. To avoid some of these difficulties it has been found expedient to consider five classes of system grounding as follows:

(*a*). Isolated neutral system.
(*b*). Ground-fault neutralizer system.
(*c*). Reactance grounded system.
(*d*). Resistance grounded system.
(*e*). Solidly grounded system.

(*a*). *Isolated-Neutral System.* For isolated-neutral systems of small capacitance to ground and small extent the maximum line-to-ground voltage for a single line-to-ground fault will be equal to the line-to-line voltage before the fault. As the capacitance to ground increases with increasing extent of the system, the maximum voltage to ground will rise above the normal line-to-line voltage and will reach very high values for systems with the ratio of X_0/X_1 in the vicinity of -2. This condition corresponds to approximate resonance in a circuit consisting of the positive- and negative-sequence reactances and the zero-sequence capacitive reactance, all in series. For a double line-to-ground fault the voltages on the sound phase will in general be less than for a single line-to-ground fault except for the range of X_0/X_1 between -2 and 0. In this range there is the possibility of resonance in a circuit consisting of the positive-sequence reactance in series with the negative-sequence and zero-sequence capacitive reactances in parallel. In considering the magnitude of the system overvoltages, it is, of course, essential to consider all circuit conditions and fault locations.[23] Usually the highest overvoltages are encountered on some abnormal condition which leaves a small amount of generating capacity connected to a system of large extent. Practical curves[8,11] for the estimation of the magnitude of overvoltages on isolated-neutral systems subjected to ground faults are available for zero and unit ratios of line resistance to X_1.

(*b*). *Ground-Fault Neutralizer System.* The voltage from a sound phase to ground on a ground-fault neutralizer system will rise to the normal line-to-line voltage in the event of a single line-to-ground fault. For double line-to-ground faults the voltages from the sound phase to ground will be less than for a single line-to-ground fault. If the ground-fault neutralizer system is out of tune, there will be some increase in the voltage to ground on the sound phase, depending upon the constants of the system and the departure from the tuned condition.

(*c, d, e*). *Impedance-Grounded Systems.* When power systems are grounded, the layouts are normally made to provide a ground-fault current which is greater than the zero-sequence charging current for normal line-to-neutral voltage on the zero-sequence network. This, of course, is done to provide sufficient current to secure positive relay action on ground faults, which is one of the principal advantages of the grounded-neutral system. When power systems are laid out in this manner, the systems generally have positive or inductive reactance from line to ground in the zero-sequence network when viewed from the fault location. This is in contrast to the negative or capacitive reactance in the zero-sequence network for the isolated-neutral system. For all impedance-grounded systems, including resistance, reactance, and solidly grounded systems, it is practical to estimate the overvoltages from sets of curves[6,8,9,11] which give the maximum line-to-ground voltage for various ratios of R_0/X_1 and X_0/X_1 for single and double line-to-ground faults.

When fault resistances are of an appreciable magnitude, their effect on the overvoltages must be taken into account.[24] Fault resistance will in some cases increase and in other cases decrease the maximum line-to-ground voltages at fundamental frequency. Curves[8,11] are available which take into account the overvoltages which are plotted on the basis of the value of fault resistance giving the highest overvoltage. When fault resistances are of important magnitude, it is necessary to select the points between which the overvoltages are to be considered. If tower-footing resistance or ground resistance at the point of fault is high, it may be necessary to distinguish between the voltages from unfaulted to faulted conductor and from unfaulted conductor to ground. Thus, for protector tubes, the important overvoltage during fault may differ from the important overvoltage for an arrester application, because in one case the fault is to the protective apparatus ground and in the other case it may not be.

Sometimes it is desirable to obtain only an indication of a ground on a system. For this purpose, a potential transformer may be connected between an available neutral point and ground, or three transformers may be connected in wye with the neutral point grounded, the secondaries being connected in wye or broken delta. The latter connection is sometimes also used for metering or relaying. Either method would fall in the class of high reactance grounding. In addition the latter method leads to the possibility of another type of overvoltage known as "neutral inversion," unless suitable means for stabilizing are employed.[26]

Transient Overvoltages on Simple Circuit Changes

The application of a ground fault to a normally balanced three-phase power system gives rise to changes in the fundamental-frequency voltage as discussed in the preceding section. In addition, there is a transient voltage produced by the circuit change which is frequently taken as the difference between the initial and final fundamental voltage. This conception is accurate for a simple circuit without loss. The overvoltages will, in general, be less than these values because of decrement and because of the resolution into oscillating components of different frequencies. However, if the change in fundamental-frequency voltage is known, the natural-frequency overvoltage superposed on the fundamental frequency can be estimated, thus providing a method of evaluation sufficient for most practical cases.

Ground Faults on Unbalanced Systems

If a power system is unbalanced with respect to ground, because of either series impedances or shunt admittances, there will be a tendency to increase the voltages on some phases and to decrease them on others. If these unbalances occur at the same time as ground faults, the voltages to ground may be importantly increased.[7] These double unbalances frequently arise as the result of a ground fault, such as may be produced by

1. A conductor breaking with grounding of the broken conductor.
2. Fuse blowing.
3. The opening of one breaker pole ahead of the others.

Such conditions may produce very high voltages to ground under conditions which would give relatively low voltages if the open-phase condition did not exist at the time of a ground fault. Sometimes the system provides a parallel resonant path from the conductors of other than the open phase through the shunting paths to the open conductor beyond the phase break to ground. Under these conditions excessively high voltages may be produced by resonance action at fundamental frequency. Each of these cases must be investigated individually, and it is not practical to set up general curves for the determination of the magnitude of system overvoltages that occur under these double unbalances. The calculation of the overvoltage for a particular case of double unbalance can be carried out by the use of the method of symmetrical components, using one of the equivalent networks for simulating the double unbalances.

COMPLEX CIRCUIT CHANGES THROUGH ARCING PATHS

Circuit changes through arcing paths may give rise to system overvoltages, either as a result of

1. Forcing current zero.
2. Restriking through the previously conducting path.

These phenomena give rise to additional transients, because of the variable resistance which is characteristic of the arc, and thus introduce additional complications in overvoltage calculations.

Forcing Current Zero

If an arcing path is rapidly deionized, there may be "forcing of current zero"[12,25] by which is meant a more rapid change of current than would occur in the circuit if certain characteristics of the arc were not present. This action produces transient voltages which increase those produced by the circuit change itself. If current is flowing through the inductive elements of the circuit at the time an arc is suppressed, there is a tendency for the current to continue, completing the circuit through shunt capacitances, thus greatly increasing the voltages across them.

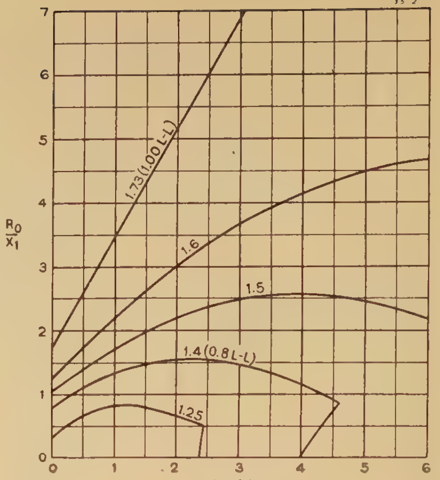

Figure 2. Line-to-ground sustained voltage chart for grounded systems, in the region of small X_0/X_1 and R_0/X_1

Figures on curves are times normal line-to-neutral voltage. In parentheses are given times line-to-line voltage for two significant values. Curves are for $X_2 = X_1$ and $R_1 = R_2 = 0$

The rate of increase of voltage and the magnitude reached depend upon the current at the time of sudden interruption, the inductance and capacitance of the circuit elements, and the dielectric strength that can be established. Such voltages can be calculated for simple circuits from the stored energy relation $LI^2 = CE^2$, using for I the value of current at the instant of sudden interruption. If considerable current is flowing and the inductance is relatively high, the transient overvoltage produced by forcing the current zero will be very large if the shunt capacitance is small. From a practical point of view, the overvoltage which may be impressed on a circuit as a result of forcing of current zero is usually limited in an arcing path because of its dielectric characteristics. The arcing path cannot instantly change from (1) low resistance corresponding to heavy current flow to (2) high dielectric strength at a very short time after the suppression of current flow. If restriking takes place, transient overvoltages may be built up or limited as discussed subsequently under the subject of "restriking".

Arcing paths in air over insulator strings and apparatus bushings rarely result in important overvoltages as a result of the forcing of current zero. However, forcing of current zero may take place in circuit breakers of either the air or oil type because of the means used to deionize the arc path rapidly. Failure of solid types of insulation may force current zero because of the high pressure built up in the fault path. In this connection it is, of course, pertinent to re-

Table I. Comparison of Various Methods of Power-System Grounding

	Isolated Neutral	Ground-Fault Neutralizer	Reactance Grounded*	Resistance Grounded	Solidly Grounded $\left(\frac{X_0}{X_1} < 3.0 \text{ and } \frac{R_0}{X_1} < 1.0 \text{ by Definition}\right)$
1. Overvoltage	Highest	Satisfactory	Satisfactory (Opinions range for generator bus from 3 to 10 and for transmission system from 3 to 40 for X_0/X_1)**	Good if neutral resistance does not exceed ground fault neutralizer ohms	Lowest
2. Ground-fault current	Very low	Negligible	Limited	Low	Highest, but seldom appreciably larger than the 3-phase short circuit current. Limit is 150 per cent of 3-phase value
3. Relaying	Difficult in most cases	Transitory line-to-ground faults are self-clearing. Relaying of other faults is satisfactory	Good	Good for low values and satisfactory for high values	Good
4. Protective level	Highest	Same as for isolated neutral	Same as for isolated neutral or better	Same as for isolated neutral or better	Lowest

* Exclusive of systems that can be classified as solidly grounded.

** Applications in the higher range may require special study.

member that, when pressure has been dissipated in this fault path, the dielectric strength is reduced below its normal value.

The interruption of current in a circuit breaker in forcing current zero is conveniently analyzed in terms of extinction voltage.[14,17] By extinction voltage is meant the voltage that appears across a circuit breaker at the end of the last half-cycle of current flow; it includes the increases in arc voltage and the increases resulting from diversion of current to shunting paths. Extinction voltages are usually relatively low but may approach the normal circuit voltage. The effect of the extinction voltage is to increase the magnitude of the transient overvoltage by an amount that is approximately equal to the extinction voltage itself.

Restriking Arcs

When current flows through any arcing path in a power system, there is a tendency for the arc to go out at every current zero and to restrike immediately or within a slight interval after normal current zero. In some cases the current is interrupted at current zero, and there is a definite "current pause" before current flow is re-established following a restrike through the arc path. This interval may be in microseconds or may last for a half-cycle of the fundamental frequency of the circuit or longer before restriking takes place.

Re-establishment of arc voltage across an arc path is controlled by the characteristic of system recovery voltage that is impressed across the arc path and tends to cause dielectric breakdown and the dielectric recovery characteristic of the arc path. If the dielectric recovery strength is at all times greater than the transient recovery voltage, then restrike will not take place. The magnitude of the resultant transient overvoltage is, therefore, greatly affected by those system characteristics that control the shape of the transient recovery voltage-time curve of the system and by the dielectric voltage-time curve of the arc path. If the arc path is of certain characteristics, restriking will take place at substantially the same potential under all conditions. Under other system transient recovery voltage and dielectric recovery characteristics, breakdown may occur under conditions for the arcs to produce cumulative oscillations[12,13] resulting in high voltage across the arcing path.

Analysis by Means of Miniature-System Setups

The preceding discussion has shown the difficulty of analyzing the effects of arcs in increasing transient voltages as a result of either forcing of current zero or as a result of restriking. In fact, the only practical method[16,18,19] available for the laboratory study of such problems is by the use of a miniature power system which is set up to simulate the actual system and which is subjected to corresponding circuit changes. In this method the various circuit elements are set up to have the corresponding positive-, negative-, and zero-sequence series impedances and shunt admittances. Distributed circuit elements, such as a transmission line, are simulated by equivalent circuits of lumped or semidistributed constants. The characteristic of the arc path can be simulated, or the arc path can be re-established as determined by the assumed dielectric characteristic of the breaker, or, as is more frequently done, the arc can be re-established at the point on the system voltage wave which would result in the highest transient disturbances. Several restrikes are possible under some conditions. However, practically all of the voltage studies to date have assumed that the probable maximum overvoltage would be obtained as a result of two restrikes occurring at such intervals as to produce the greatest possible rate of increase in system overvoltage. In the operation of a circuit breaker many restrikes may occur in the process of opening. In this connection it is to be pointed out that unless the magnitude and time of the dielectric recovery voltage curve have just the right values to cause breakdown by system recovery voltage, the restrikes will not occur at such intervals as to produce maximum cumulative action. Frequently the transient overvoltages, which cannot be avoided on the energizing of a circuit, are comparable to the voltages produced on opening a circuit with many restrikes.

System overvoltages as a result of arcing faults to ground or arcs through switching equipment in the process of opening or closing the circuit are affected by the method of system grounding and the magnitude of the grounding impedance, because these voltages are controlled by the magnitude of the sequence-impedance constants to ground and their relation to the normal circuit impedance.

Miniature-system setups have been used for the study of transients on systems grounded in different ways.[8,16,19] These investigations of the transient overvoltages for single or double line-to-ground faults or switching operations on ground-faulted systems show that the solidly grounded system is the best, and the isolated system is the worst. In addition, the investigations of ground-fault neutralizer systems show that

1. For arcing faults to ground that are cleared without switching, the transient overvoltages are very low.
2. For switching a grounded line, the transient voltages are comparable to those of an isolated-neutral system.*

For ground faults on reactance-grounded systems, the range of transient voltages has two minimum values corresponding to the solidly grounded system and to the ground-fault neutralizer system, and two maximum values, one corresponding to the isolated-neutral system and the other corresponding to a reactance intermediate between zero for the solidly grounded system and the ground-fault neutralizer value. With resistance-grounded systems of low ohmic value, the transient voltages approach those of the solidly grounded system, and with high ohmic values (higher than ground-fault neutralizer values), the voltages approach those corresponding to the isolated-neutral system.

Summary

The foregoing discussion of system overvoltages indicates broadly the scope and complexity of the problem. In this section of the report an attempt will be made to present as concisely as possible the relationship between system overvoltages and the method of neutral grounding.

In the preceding classification of the causes of system overvoltage (see section on causes of system overvoltage) items 1*a*, *b*, and *c* deal with phenomena which are independent of the method of system grounding.

Items 2*a*, *b*, and *c* do not lend themselves to a generalized relationship with neutral-grounding impedances. However, it can be said that the isolated-neutral system is likely to give more trouble from this source than any other type of system.

The overvoltage most frequently encountered in power system operation is that which occurs as a consequence of a fault to ground (items 3*a* and *b*). These overvoltages are definitely related to the amount of grounding impedance as indicated in Figure 1. The curves of this figure are based on the sequence series reactances X_1, X_2, X_0, the zero-sequence resistance R_0, the sequence shunt capacitances C_1, C_2, C_0, and the values of fault resistance R_f, all viewed from the point of fault. The values of negative-sequence reactance and shunt capacitance are taken, in line with conventional practice, as equal to the corresponding positive-sequence quantities, although slightly higher values of voltage would be obtained if X_2 were greater than X_1. The solid curves of Figure 1 show the sustained fundamental-frequency voltages (neglecting decrement in machine fluxes) that appear from line to ground for a single line-to-ground fault. These curves are important mainly, because they are the basis for determining arrester ratings required for a particular system. A double line-to-ground fault need not be considered generally in the practical range of system constants, since voltages to ground are less than for a line-to-ground fault except as indicated in the discussion of the curves of Figure 2. If X_0/X_1 is small and positive, sustained overvoltages resulting from faults are low when R_0/X_1 is also low.

The region in which X_0/X_1 and R_0/X_1 are both small is of considerable importance. This region has been plotted in Figure 2 in a different way[11] for the purpose of showing more clearly the effect of zero-sequence impedance upon the maximum sustained voltages to ground for any type of fault and for any value of fault resistance (in Figure 1 fault resistance was neglected for positive values of X_0/X_1). These curves are for $X_2=X_1$ and $R_1=R_2=0$. They show the maximum zero-sequence impedance ratios R_0/X_1 and X_0/X_1 for which a definite voltage will not be exceeded between any phase and ground.

The curves of Figure 2 are irregular, because they are composed of parts that represent limiting voltages on different phases or for different types of faults. Thus, assuming counterclockwise rotation in *a*, *b*, *c* order, the principal parts of the curves are arcs of circles corresponding to the voltage on phase *c* for a single line-to-ground fault on phase *a* with zero fault resistance. For parts of the 1.25 and 1.40 curves near the R_0/X_1 axis, the effect of introducing fault resistance is to increase the voltage on phase *c*. For the 1.25 and 1.4 curves the portions between the X_0/X_1 axis and the points of abrupt change in the curves are determined by the voltage on phase *b*, for a single line-to-ground fault on phase *a* with the fault resistance such as to give maximum voltage. Part of the 1.25 curve near the X_0/X_1 axis is determined by the voltage on phase *a* for a double line-to-ground fault on phases *b* and *c*.

With reference to Figure 1 again, it is evident that sustained overvoltages can be very high if the ratio X_0/X_1 is in the vicinity of -2. However, this is of little practical significance, since systems would not normally operate with such constants. Most systems normally operated with isolated neutral (X_0/X_1 negative) would have a negative value of X_0/X_1 greater than 40. Consequently the region of highest voltages would not be of importance except possibly during a system disturbance in the course of which a large portion of generating capacity is lost, or the major neutral grounding points are separated from the system as a result of switching.

Isolated neutral systems have negative values of the ratio X_0/X_1 which usually are in the range between -40 and minus infinity. From Figure 1 it will be seen that for a ground fault the fundamental-frequency sustained voltage from line to ground equals 1.73 times line-to-ground voltage for the ratio of X_0/X_1 equal to minus infinity, and, that as the ratio is decreased to -40, the voltage increases to about five per cent above this value. For ground faults on isolated-neutral systems the effect of fault resistance (item 3*b* in the previous classification) causes further increase in the voltages to ground. The maximum sustained voltages to ground for any value of fault resistance are given in Figure 1 by the curve made up of long dashes separated by two short ones. In the application of protective devices, such as lightning arresters, this circumstance theoretically requires the use of devices with ratings higher than the emergency-rated voltage of the system. For some isolated-neutral systems this procedure is necessary. However, there exists considerable experience on many isolated-neutral systems with lightning arresters of ratings equivalent to the emergency-rated circuit voltage. The damage to the arrester resulting from this practice has usually been negligible, and it is probably more economical to take the risk of an occasional lightning-arrester failure from this cause than to apply arresters of higher rating.[10,11]

When a fault suddenly occurs on one phase of a system, a transient voltage occurs on the other phases. The maximum transient voltage depends on the ratio of C_1/C_0, system configuration, the amount of reactance in the circuit (X_0/X_1), the amount of resistance in the circuit (R_0/X_1), and the instant at which the fault occurs relative to the voltage wave, a particular instant being required to give the maximum disturbance. The effects of these factors are indicated in Figure 1, and from them an estimate can be made of the maximum transient voltage that can be obtained for most system operating conditions. The curves in Figure 1 made up of long dashes separated by one short dash are for $R_0/X_1=0$. In particular, it should be noted that as C_0 increases relative to C_1, the maximum transient voltage increases. In most cases C_1 will be greater than C_0, however.

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* This of course does not apply when the ground-fault neutralizer is short-circuited before switching is done.

Zero-sequence resistance (R_0/X_1) is seen to be helpful in reducing these transient oscillations. Curves for $C_1=C_0$ and for various ratios of R_0/X_1 are shown as a series of short dashes in Figure 1. While no curves are shown for values of R_0/X_1 other than zero for positive values of X_0/X_1, any resistance will tend to reduce the overvoltage from the maximum values shown for $R_0/X_1=0$.

Items 3*c*, *d*, and *e* do not lend themselves to generalized summarizing, such as shown by the curves for items 3*a* and *b*. Any of these items may be present to a certain degree and may in some cases add considerably to the amount of overvoltage shown by the curves of Figures 1 and 2.

Switching operations collectively (item 4) cannot be related definitely to system-grounding impedances. The isolated-neutral system on the one extreme is subject to the highest switching surges, while the solidly grounded system on the other extreme is subject to the lowest switching surges, generally speaking. Studies of particular systems and particular switching operations have been made, and these are helpful in pointing to circuit conditions and methods of grounding to be avoided. Certain values of neutral grounding reactance have been shown to give rise to high transient voltages during switching.[16–19] Relatively high values of neutral grounding resistance are permissible without encountering dangerous overvoltages.[17,19] In cases for which the published results are not sufficiently indicative, it is recommended that further detailed studies be made. In many cases, the *probability* of occurrence may be small, although the *possibility* of getting destructive voltages of the types classified earlier in this report may actually exist.

Of the influences listed under item 5, only "(*c*). Electrostatic charging of ungrounded systems by wind-borne particles, . . ." is recognized generally as a function of grounding impedance. Cases in which conduction or induction is a recognized cause should be studied individually.

It should be evident that it is not possible to specify any specific number of ohms of resistance or reactance as being adequate for grounding and for eliminating the possibility of all overvoltages, since the magnitude and arrangement of other system constants is directly involved. However, it is possible to limit the overvoltages and thus protect apparatus by means of lightning arresters. Arresters respond to voltage and make no distinction as to whether the cause is lightning or a switching operation. In any case, a voltage protective level is held. In the case of a sufficiently low grounding impedance, permitting the use of grounded neutral rating arresters, this protective level is substantially lower than for higher grounding impedance requiring the ungrounded rating arrester. Consequently there is a gain, from the standpoint of lowering the protective level, in selecting grounding impedances of low ohmic value. However other considerations, such as limitation of short-circuit currents for power-system operating reasons or for inductive coordination, may prevent taking full advantage of this possibility.

It should be emphasized that the number of grounding points is of importance also, since during a system disturbance it is undesirable to have any part of the system with connected generation to be left without a neutral ground. While in the limit, this would mean that every source of short-circuit current should have a neutral ground, practical and economic considerations may dictate a somewhat smaller number of grounding points.

Engineering opinions, as to the choice of resistance or reactance grounding and the proper ohmic value, have not crystallized. This situation results from the complexity of the problem, the intangibility of many factors involved, the range of conditions encountered, and the results to be achieved. However, it is pertinent in connection with this discussion to consider the effect of such factors in the grounding problem as relative dielectric strength of apparatus, current limitation requirements, relaying requirements, and lightning-arrester settings, since they affect the kind and magnitude of grounding impedances.

Many engineers prefer to ground directly all available neutral points, introducing neutral reactance where necessary to avoid increasing machine stresses and breaker ratings on ground faults beyond the values corresponding to three-phase short-circuit currents. This procedure comes within the classification of solidly grounded systems with $X_0/X_1<3$. Other engineers prefer to introduce greater limitation of fault current, which is generally accomplished by neutral resistors. The problems are different for grounding transmission systems through transformers than for grounding generator busses because of the relatively lower dielectric strength of rotating machines. In general, the transient overvoltages with resistance grounding are more favorable than with moderate values of reactance grounding, and the maximum value of resistance which may be used is generally considered to be that which corresponds to the tuned reactance value of the ground-fault neutralizer. The maximum permissible value of reactance grounding is considerably more speculative, and discussions have not reached a conclusion. The permissible value of the ratio X_0/X_1 is affected by the extinction voltage, the sequence series reactance and sequence shunt capacitance constants of the system, since they affect the conditions for which a high-frequency current zero may be obtained and thus control the conditions for restriking. Engineering opinion on the maximum permissible value of X_0/X_1 ranges from 3 to 10 for generator grounding. For grounding transmission systems, higher ratios of X_0/X_1 have been considered, ranging from 10 to 40, the highest values having been considered for grounding transformers applied to transmission circuits with aerial lines. Applications in this range however may require special study. Sometimes resistors in parallel with neutral reactors have been used, with the reactor to limit ground current and the resistor to limit transient voltages.

Table I lists in a very general way the advantages and disadvantages of various methods of system grounding.

Classification of Power Systems With Respect to Method of Grounding

The preceding discussion has shown that, from the standpoint of overvoltages at least, the various methods of grounding power systems may be classified according to ground connections and sequence impedance constants* as follows:

1. *An isolated-neutral (ungrounded) system* is a system without an intentional connection to ground. Isolated-neutral systems may be subjected to various overvoltages, depending upon the ratio of X_0/X_1 as viewed from the fault location.**

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* R_0, X_0, and X_1 are, respectively, the zero-sequence resistance, the zero-sequence reactance taken as positive if inductive and negative if capacitive, and positive-sequence subtransient reactance. All these impedances are those of the system as viewed from the fault location.

** A system grounded through potential transformers is not an isolated-neutral but a reactance-grounded system, according to this classification.

2. *A grounded system* is a system in which at least one conductor or point (usually the middle wire or neutral point of transformer or generator windings) is intentionally grounded, either solidly or through impedance. Grounded systems may be subjected to various overvoltages, depending upon the ratios of X_0/X_1 and R_0/X_1 as viewed from the fault location.

3. *A solidly grounded system or portion of a system* is a system or specified portion thereof that has for all fault locations zero- to positive-sequence ratios of $X_0/X_1<3$ and $R_0/X_1<1$ for any condition of operation and amount of connected generator capacity.[8,10,11,20]

4. *A resistance-grounded system* is a system grounded through an impedance, the principal element of which is resistance.

5. *A reactance-grounded system* is a system grounded through an impedance, the principal element of which is reactance.

6. *A ground-fault neutralizer system* is a system grounded by means of a grounding device which provides an inductive reactive component of current in a ground fault that is substantially equal to, and therefore neutralizes, the rated frequency capacitive component of the ground-fault current.

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Equivalent Circuits for Oscillating Systems and the Riemann–Christoffel Curvature Tensor

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Synopsis: Many electromechanical systems that are being studied in connection with the war effort have so many degrees of freedom that it is totally impractical to analyze their performance by direct calculation. It is, therefore, increasingly important to develop equivalent circuits for these multiple oscillating systems, that can be put on the a-c calculating board and so solved by direct reading of instruments.

In this paper it is shown that a necessary (though not sufficient) condition for the existence of a physical model, corresponding to a given set of equations, is that the set should be a tensor equation. This follows from the fact that only quantities that are tensors can be measured by instruments. The conclusion is therefore reached that only equations that are in tensor form can be set up on the a-c calculating board.

The principle is illustrated by setting up equivalent circuits with the aid of tensorial hunting equations for the determination of the steady-state, hunting, and self-excitation performance of two interconnected instrument-Selsyns, of two salient-pole synchronous machines, and of a capacitor-compensated transmission line connected to a salient-pole synchronous machine. A companion paper, "Self-Excited Oscillations of Capacitor-Compensated Long-Distance Transmission Systems"[12] contains the results of an extended study made on the a-c network analyzer with the aid of one of the equivalent circuits developed here.

Difficulties in Establishing Equivalent Circuits for Hunting

IN trying to set up equivalent stationary networks for the *hunting* performance calculation of interconnected systems, it was found to be impossible to establish networks that correspond to the equations of the systems, even though the latter were sets of linear algebraic equations of the form $\boldsymbol{e}=\boldsymbol{Z}\cdot\boldsymbol{i}$. All known methods of tranformation of reference frames or of variables led to expressions that could not be measured by instruments.

The apparently insurmountable difficulties were finally overcome by the following reasoning:

The equations of small oscillation under consideration were the standard Lagrangean equations of motion, in which x^α was replaced by $x^\alpha+\Delta x^\alpha$ as is the universal custom. It is however well known that, while the Lagrangean equations are tensor equations, the equations of small oscillations, derived from the former by standard methods, are no longer tensor equations. Since a tensor is a measurable physical entity, it seems plausible that the standard equations of hunting do not show correctly the physics of the problem—even though they do give correct numerical answers—nor do they express the hunting phenomena in terms of measurable quantities. Hence, no physical model corresponding to a set of nontensor equations may exist.

The tensorial form of the equations of small oscillations of electrical machinery already has been established[8]. These tensor equations differ from the standard equations by having certain terms added to and subtracted from the latter. In other words, if the standard hunting equations are expressed as

$$\boldsymbol{A}+\boldsymbol{B}=0$$

(where none of the sets of expressions $\boldsymbol{A}$ or $\boldsymbol{B}$ nor the combination $\boldsymbol{A}+\boldsymbol{B}$ are tensors), the tensorial hunting equations may be expressed as

$$\boldsymbol{A}+\boldsymbol{B}+\boldsymbol{C}-\boldsymbol{C}=0$$

where $\boldsymbol{C}$ represents a set of expressions, that otherwise would cancel.

Now while neither $\boldsymbol{A}$ nor $\boldsymbol{C}$ are tensors, the combination $\boldsymbol{A}+\boldsymbol{C}$ is a tensor, so is the combination $\boldsymbol{B}-\boldsymbol{C}$. Hence each expression in the equation

$$(\boldsymbol{A}+\boldsymbol{C})+(\boldsymbol{B}-\boldsymbol{C})=0$$

is a tensor. Therefore $\boldsymbol{A}+\boldsymbol{C}$ must be a measurable quantity and so must $\boldsymbol{B}-\boldsymbol{C}$.

If the reasoning is valid then this last equation and not the simplified equation must be the proper physical form, that admits an equivalent circuit. If $\boldsymbol{C}$ is cancelled, then the resultant equation still would give correct numerical answers, though it would represent an incomplete physical picture, hence a physically unrealizable equivalent circuit.

This reasoning proved to be correct in every detail. When the additional terms $\boldsymbol{C}$ were calculated, the equivalent circuit of the oscillating system followed immediately and in a perfectly straightforward manner.

In machines for which the standard hunting equations did give an equivalent circuit outright, the terms $\boldsymbol{A}$ and $\boldsymbol{B}$ each were already tensors (measurable quantities) because of the simplicity of the physical system. Hence the hunting equation itself $\boldsymbol{A}+\boldsymbol{B}=0$ was already a tensor equation.

The First Principle of the General Theory of Relativity

In 1905 Einstein came forward with the special theory of relativity. One of the consequences of this theory is the postulate that the three co-ordinates of space x, y, z, and time t are not two distinct physical entities

$$x^m=\begin{array}{c|c|c|c} \diagdown m & 1 & 2 & 3 \\ \hline & x & y & z \end{array} \qquad t=\boxed{t}$$

but are components of a single tensor, a single physical entity

$$x^\alpha=\begin{array}{c|c|c|c|c} \diagdown \alpha & 1 & 2 & 3 & 4 \\ \hline & x & y & z & jct \end{array}$$

With this assumption it could be shown, for instance, that the separate electric and magnetic fields were only components of one underlying physical reality, the electromagnetic field. (Namely, $\boldsymbol{B}=B_\alpha$ and $\boldsymbol{H}=H^\alpha$ are components of one tensor $F_\alpha^{\cdot\beta}$.)

In 1916 Einstein announced the general theory of relativity. One of the outgrowths of this more general theory is the postulate that both the gravitational and the electromagnetic fields are components of only one underlying physical reality. The first principle of this theory may be formulated for the present purpose as follows:[1]

"All laws of nature are tensor equations"*

For instance, the equations of Lagrange are tensor equations; so are the field equations of Maxwell in special reference frames. For more general reference frames Maxwell's equations have been brought into tensorial form by Minkowsky.

The question whether the group of transformations under which the laws of

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* "Die allgemeinen Naturgesetze sind durch Gleichungen auszudrucken, die für alle Koordinatensysteme gelten, d.h. die beliebigen Substitutionen gegenüber kovariant (allgemein kovariant) sind."

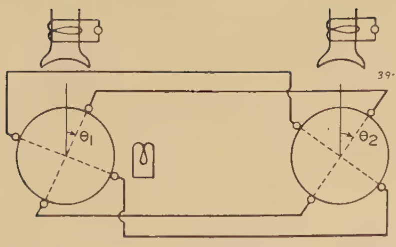


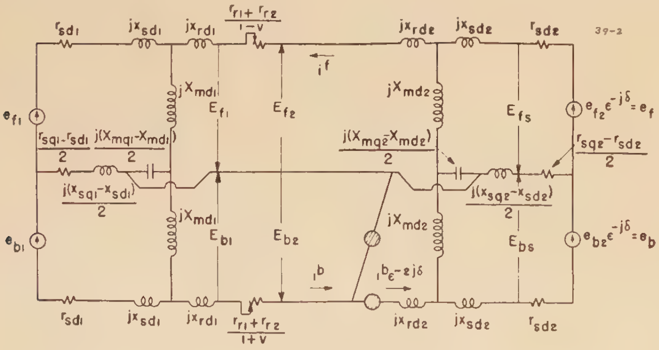
Figure 1. Two single-phase instrument Selsyns

nature are invariant is the Lorentz group (as postulated by Einstein), or the Galilean group, has no bearing upon the argument of this paper.

A Principle to Establish Models for Physical Systems

Of course not only the grandiose differential equations that describe the curvature of light as it passes near the sun are tensor equations, but also the simple linear differential or algebraic equations that represent the performance of such a lowly creation as a stationary network. Hence if a stationary network is given, and its set of linear algebraic equations $\boldsymbol{e}=\boldsymbol{Z}\cdot\boldsymbol{i}$ is established by Ohm's and Kirchhoff's laws, the set always comes out as a tensor equation.

But let now the problem be reversed. Let a set of linear algebraic equations $\boldsymbol{e}=\boldsymbol{Z}\cdot\boldsymbol{i}$ be given: Are or are they not the equations of a stationary network? In other words, is it possible to establish a physical model whose performance corresponds term by term to the given set of equations?


39-2
r_{sd1}
jx_{sd1}
jx_{rd1}
$\frac{r_{r1}+r_{r2}}{1-v}$
jx_{rd2}
jx_{sd2}
r_{sd2}
i^f
jX_{md1}
jX_{md2}
e_{f1}
E_{f1}
E_{f2}
E_{fs}
$e_{f2}\epsilon^{-j\delta}=e_f$
$\frac{r_{sq1}-r_{sd1}}{2}$
$\frac{j(X_{mq1}-X_{md1})}{2}$
$\frac{j(X_{mq2}-X_{md2})}{2}$
$\frac{r_{sq2}-r_{sd2}}{2}$
$\frac{j(x_{sq1}-x_{sd1})}{2}$
$\frac{j(x_{sq2}-x_{sd2})}{2}$
e_{b1}
E_{b1}
E_{b2}
E_{bs}
$e_{b2}\epsilon^{-j\delta}=e_b$
i^b
$i^b\epsilon^{-2j\delta}$
$\frac{r_{r1}+r_{r2}}{1+v}$

a) Steady-State Network

Table I. Torque Formulas of Two Instrument Selsyns

Valid If Either Machine Hunts

(a). **Steady-State Torques**

$i^f E_{f1}{}^* = W_1$ $\quad i^f E_{f2}{}^* = W_3$ $\quad T_1 = W_1 + W_2$

$i^b E_{b1}{}^* = W_2$ $\quad i^b E_{b2}{}^* = W_4$ $\quad T_2 = W_3 + W_4$

(b). **Damping and Synchronizing Torques of the Second Machine (Receiver)**

$i^f \Delta E_{f2}{}^* = W_5 + jQ_5$ $\quad i^b \Delta E_{b2}{}^* = W_7 + jQ_7$ $\quad T_{s2}' = \dfrac{W_5+W_6+W_7+W_8}{2}$

$\Delta i^f E_{f2}{}^* = W_6 + jQ_6$ $\quad \Delta i^b E_{b2}{}^* = W_8 + jQ_8$ $\quad T_{d2}' = \dfrac{(Q_6+Q_8)-(Q_5+Q_7)}{2h}$

Repeat the same measurements with $-h$ hunting network.

$T_{s2} = T_{s2}' + T_{s2}''$ $\quad T_{d2} = T_{d2}' - T_{d2}''$

(c). **Damping and Synchronizing Torques of the First Machine (Transmitter)**

$i^f \Delta E_{f1}{}^* = W_9 + jQ_9$ $\quad i^b \Delta E_{b1}{}^* = W_{11} + jQ_{11}$ $\quad T_{s1}' = \dfrac{W_9+W_{10}+W_{11}+W_{12}}{2}$

$\Delta i^f E_{f1}{}^* = W_{10} + jQ_{10}$ $\quad \Delta i^b E_{b1}{}^* = W_{12} + jQ_{12}$ $\quad T_{d1}' = \dfrac{(Q_{10}+Q_{12})-(Q_9+Q_{11})}{2h}$

Repeat the same measurements with $-h$ hunting network.

$T_{s1} = T_{s1}' + T_{s1}''$ $\quad T_{d1} = T_{d1}' - T_{d1}''$

In consequence of the first principle of general relativity, it is safe to conclude that a primary and necessary condition for the establishment of a model is that the set of equations should be a tensor equation. If it is not a tensor equation, then the set definitely cannot represent any physically existing structure, such as a stationary network.

This deduction was rather emphatically proved in attempting to establish equivalent circuits by use of the nontensor hunting equations $\boldsymbol{e}=\boldsymbol{Z}\cdot\boldsymbol{i}$.

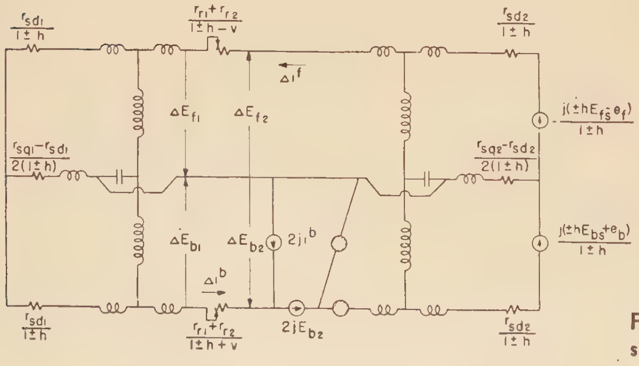
This simple fact may be generalized to include all physical phenomena by the following principle:

> "A set of equations, representing the performance of any physical system (be it mechanical, electrical, thermal, or any other system) may be represented by a physical model (equivalent circuit) only if the set of equations is a tensor equation."

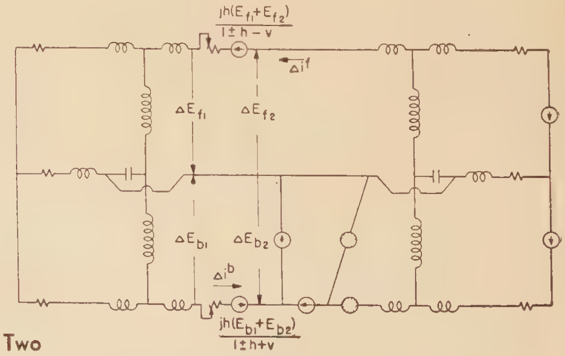
Even if no model of a system is built, the engineer is interested in the objects that he can measure. Or if he does not care to measure certain quantities, he may still be interested in a mental "physical picture" of the phenomena taking place in the various parts of the system. As a corollary of the above principle it may be stated that:

> "In order that a set of equations should express the performance of a physical system in terms of measurable and visualizable quantities, the set of equations must be a tensor equation."

Examples of sets of nontensor equations are those that represent small oscillations of dynamical systems, if these equations


$\frac{r_{sd1}}{1\pm h}$
$\frac{r_{r1}+r_{r2}}{1\pm h-v}$
$\frac{r_{sd2}}{1\pm h}$
Δi^f
ΔE_{f1}
ΔE_{f2}
$\frac{j(\pm hE_{fs}-e_f)}{1\pm h}$
$\frac{r_{sq1}-r_{sd1}}{2(1\pm h)}$
$\frac{r_{sq2}-r_{sd2}}{2(1\pm h)}$
ΔE_{b1}
ΔE_{b2}
$2ji^b$
$\frac{j(\pm hE_{bs}+e_b)}{1\pm h}$
Δi^b
$\frac{r_{r1}+r_{r2}}{1\pm h+v}$
$2jE_{b2}$

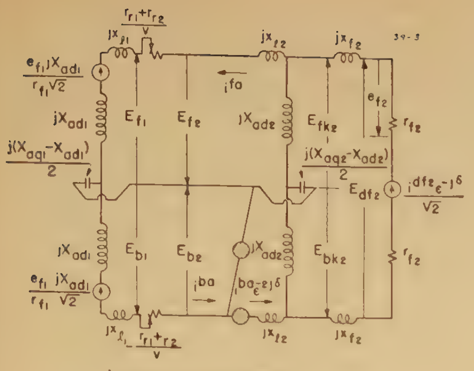
b) The Second Machine (Receiver) Hunts



c) The First Machine (Transmitter) Hunts

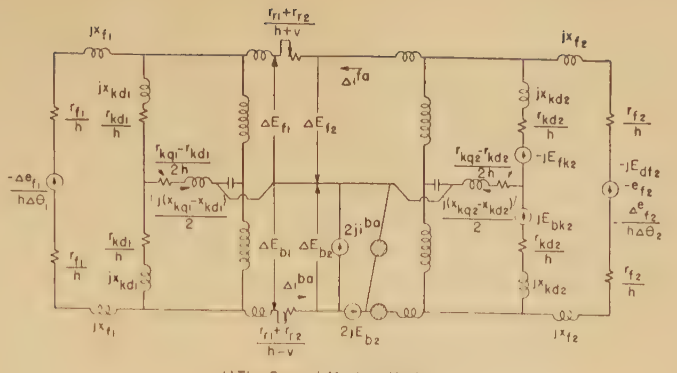
Figure 2. Two single-phase instrument Selsyns

Figure 3. Two salient-pole synchronous machines

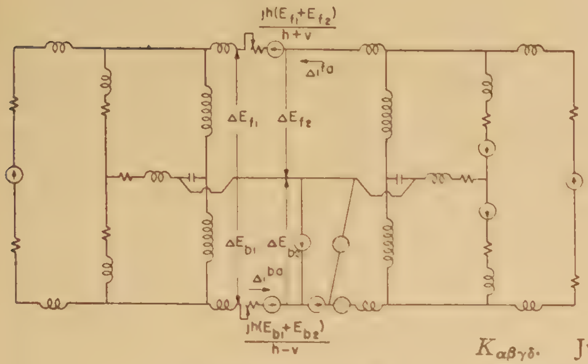


$\frac{r_{f1}+r_{f2}}{v}$
jx_{f1}
jx_{f2}
$\frac{e_{f1}jX_{ad1}}{r_{f1}\sqrt{2}}$
jX_{ad1}
E_{f1}
E_{f2}
i^{fa}
jX_{ad2}
E_{fk2}
e_{f2}
r_{f2}
$\frac{j(X_{aq1}-X_{ad1})}{2}$
$\frac{j(X_{aq2}-X_{ad2})}{2}$
E_{df2}
$\frac{i^{df2}\epsilon^{-j\delta}}{\sqrt{2}}$
E_{b1}
E_{b2}
E_{bk2}
i^{ba}
$i^{ba}\epsilon^{j\delta}$

a) Steady-State Network



b) The Second Machine Hunts



c) The First Machine Hunts

are derived from the equation of Lagrange by standard methods. Other nontensor equations are those of magnetrons, ultra-high-frequency tubes, and so forth, in the analysis of which small variations are taken in Maxwell's equations.

Many of the theories used in electrical engineering for the study of rotating machines and stationary networks are also nontensor equations. Such theories are usually valid for one particular machine and even then only for one particular reference frame. In contrast with these in Park's analysis of the synchronous machine, all basic equations (except those of hunting) may be shown to be tensor equations, and to this very fact their success must be attributed. Fortescue's method of symmetrical components is a tensorial transformation; hence its value and permanence.

The conclusion of the reasoning is that even though a set of linear algebraic equations $\boldsymbol{e}=\boldsymbol{Z}\cdot\boldsymbol{i}$ is available, it does not follow that an equivalent stationary network can be established for it. A necessary (though not sufficient) condition is that the equation should be a tensor equation. If it is not, then it cannot correspond term by term to any physically realizable network.

The change of a set of linear algebraic equations $\boldsymbol{e}=\boldsymbol{Z}\cdot\boldsymbol{i}$ into a tensor equation consists—in the present case when it represents small oscillations—of the introduction of such tensorial concepts as the Riemann–Christoffel curvature tensor $K_{\alpha\beta\gamma\delta}$. Just how to change a set of equations into a tensor equation always depends on what the equations represent physically. The equations of each type of physical phenomenon require separate treatment.

The Affine Connection

The most general form of connection used in affine differential geometry and also in the study of electrical machinery is

$$\Gamma_{\alpha\beta,\gamma}=\frac{1}{2}\left(\frac{\partial a_{\beta\gamma}}{\partial x^{\alpha}}+\frac{\partial a_{\gamma\alpha}}{\partial x^{\beta}}-\frac{\partial a_{\alpha\beta}}{\partial x^{\gamma}}\right)-\Omega_{\alpha\beta,\gamma}+\Omega_{\gamma\beta,\alpha}+\Omega_{\gamma\alpha,\beta}+S_{\alpha\beta\gamma}-S_{\gamma\beta\alpha}-S_{\gamma\alpha\beta}+\frac{1}{2}(Q_{\alpha\beta\gamma}+Q_{\beta\gamma\alpha}-Q_{\gamma\alpha\beta}) \qquad (1)$$

where

$S_{\alpha\beta\gamma}$ = torsion tensor, skew-symmetric in α and β

$Q_{\alpha\beta\gamma}$ = "Weyl tensor," symmetric in α and β

$\Omega_{\alpha\beta,\gamma}$ = nonholonomic object, skew-symmetric in α and β

In the special types of electrical machines considered in this paper the Weyl tensor $Q_{\alpha\beta\gamma}$ is zero.

In the dynamical equations of Lagrange $\Gamma_{\alpha\beta,\gamma}$ is always multiplied by $\dot{x}^{\alpha}=i^{\alpha}$ twice, as $\Gamma_{\alpha\beta,\gamma}i^{\alpha}i^{\beta}$. As a consequence some of the above terms cancel or become equal, so that the affine connection simplifies to

$$\Gamma_{\alpha\beta,\gamma}=\left(\frac{\partial a_{\beta\gamma}}{\partial x^{\alpha}}-\frac{1}{2}\frac{\partial a_{\alpha\beta}}{\partial x^{\gamma}}\right)+2\Omega_{\gamma\alpha\beta}-2S_{\gamma\alpha\beta}$$
$$=[\alpha\beta,\gamma]-2S_{\gamma\alpha\beta} \qquad (2)$$

where $[\alpha\beta, \gamma]$ is the *nonholonomic* Christoffel symbol.

It is emphasized that the above simplified form of $\Gamma_{\alpha\beta,\gamma}$ can be used only in the equation of motion and in its variations, such as the equations of small oscillations. In any other equation, such as the field equations of Maxwell, the complete form of $\Gamma_{\alpha\beta,\gamma}$, as it appears in equation 1 must be used.[9]

The Three Types of Transformations

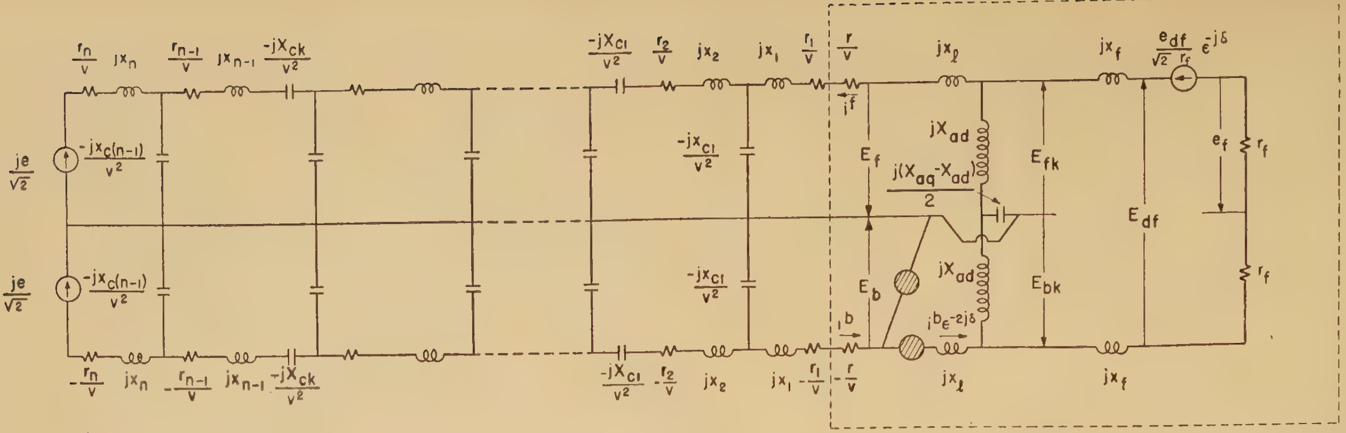
In establishing equivalent networks for electrical machines there are at least three types of transformations used:[2]

1. Co-ordinate transformations.

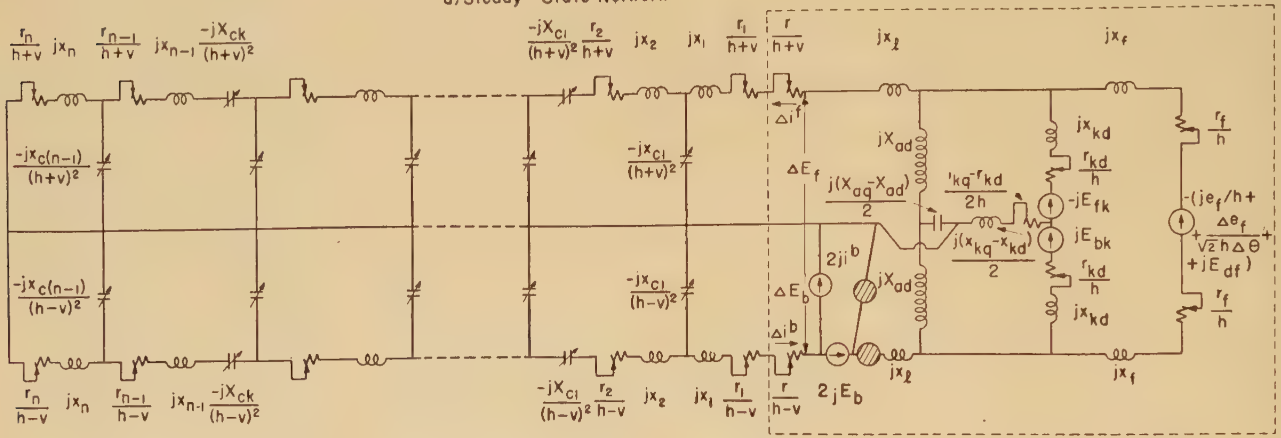
Table II. Torque Formulas of Two Synchronous Machines

Valid If Either Machine Hunts

Steady-State Torques	Damping and Synchronizing Torques	
First Machine	**First Machine**	**Second Machine**
$i^{fa}E_{f1}^{*}=W_1$	$i^{fa}\Delta E_{f1}^{*}=W_5+jQ_5$	$i^{fa}\Delta E_{f2}^{*}=W_9+jQ_9$
$i^{ba}E_{b1}^{*}=W_2$	$\Delta i^{fa}E_{f1}^{*}=W_6+jQ_6$	$\Delta i^{fa}E_{f2}^{*}=W_{10}+jQ_{10}$
$T_1=W_1+W_2$	$i^{ba}\Delta E_{b1}^{*}=W_7+jQ_7$	$i^{ba}\Delta E_{b2}^{*}=W_{11}+jQ_{11}$
	$\Delta i^{ba}E_{b1}^{*}=W_8+jQ_8$	$\Delta i^{ba}E_{b2}^{*}=W_{12}+jQ_{12}$
Second Machine		
$i^{fa}E_{f2}^{*}=W_3$		
$i^{ba}E_{b2}^{*}=W_4$	$T_{s1}=-(W_5+W_6+W_7+W_8)$	$T_{s2}=-(W_9+W_{10}+W_{11}+W_{12})$
$T_2=W_3+W_4$	$T_{d1}=\frac{(Q_5+Q_7)-(Q_6+Q_8)}{h}$	$T_{d2}=\frac{(Q_9+Q_{11})-(Q_{10}+Q_{12})}{h}$


$\frac{r_n}{v}$ jx_n $\frac{r_{n-1}}{v}$ jx_{n-1} $\frac{-jX_{ck}}{v^2}$
$\frac{-jX_{c1}}{v^2}$ $\frac{r_2}{v}$ jx_2 jx_1 $\frac{r_1}{v}$ $\frac{r}{v}$
jx_l jx_f $\frac{e_{df}}{\sqrt{2}\,r_f}\epsilon^{-j\delta}$
$\frac{je}{\sqrt{2}}$ $\frac{-jx_{c(n-1)}}{v^2}$
i^f E_f jX_{ad} $\frac{j(X_{aq}-X_{ad})}{2}$ E_{fk} e_f r_f E_{df}
E_b i^b $i^b\epsilon^{-2j\delta}$ E_{bk}
$-\frac{r_n}{v}$ $-\frac{r_{n-1}}{v}$ $-\frac{r_2}{v}$ $-\frac{r_1}{v}$ $-\frac{r}{v}$

a) Steady-State Network


$\frac{r_n}{h+v}$ jx_n $\frac{r_{n-1}}{h+v}$ jx_{n-1} $\frac{-jX_{ck}}{(h+v)^2}$
$\frac{-jX_{c1}}{(h+v)^2}$ $\frac{r_2}{h+v}$ jx_2 jx_1 $\frac{r_1}{h+v}$ $\frac{r}{h+v}$
jx_l jx_f
$\frac{-jx_{c(n-1)}}{(h+v)^2}$ $\frac{-jx_{c(n-1)}}{(h-v)^2}$ $\frac{-jx_{c1}}{(h+v)^2}$ $\frac{-jx_{c1}}{(h-v)^2}$
Δi^f ΔE_f jX_{ad} $\frac{j(X_{aq}-X_{ad})}{2}$ $\frac{r_{kq}-r_{kd}}{2h}$ $\frac{j(x_{kq}-x_{kd})}{2}$
jx_{kd} $\frac{r_{kd}}{h}$ $-jE_{fk}$ jE_{bk} $\frac{r_f}{h}$
$-(je_f/h+\frac{\Delta e_f}{\sqrt{2}h\Delta\theta}+jE_{df})$
$2ji^b$ ΔE_b Δi^b $2jE_b$
$\frac{r_n}{h-v}$ $\frac{r_{n-1}}{h-v}$ $\frac{-jX_{ck}}{(h-v)^2}$ $\frac{-jX_{c1}}{(h-v)^2}$ $\frac{r_2}{h-v}$ $\frac{r_1}{h-v}$ $\frac{r}{h-v}$

b) Hunting Network

Figure 4. Transmission line with series capacitors connected to salient-pole synchronous machine

All reference axes rotate uniformly with the infinite bus

2. Spin transformations.
3. Gauge transformations.

Co-ordinate transformations are used in passing from the stationary ***a, b, c*** axes to the rotating ***d, q, o*** axes or in interconnecting the reference axes. Spin transformations are employed in introducing the hypothetical symmetrical component frame. Gauge transformations enter in eliminating some of the phase shifters that would otherwise appear in the equivalent circuits.

In consequence of all these necessary transformations, the theories of spinor analysis must be resorted to, in addition to those of tensor analysis.

Table III. Torque Formulas of Synchronous Machine Connected to Transmission Line

Steady-State Torques	Damping and Synchronizing Torques		
$i^f E_f^* = W'$	$i^f \Delta E_f^* = W_1 + jQ_1$	$\Delta i^f E_f^* = W_3 + jQ_3$	$T_s = W_1 + W_2 + W_3 + W_4$
$i^b E_b^* = W''$	$i^b \Delta E_b^* = W_2 + jQ_2$	$\Delta i^b E_b^* = W_4 + jQ_4$	$T_d = \frac{(Q_3+Q_4)-(Q_1+Q_2)}{h}$
$T = W' + W''$			

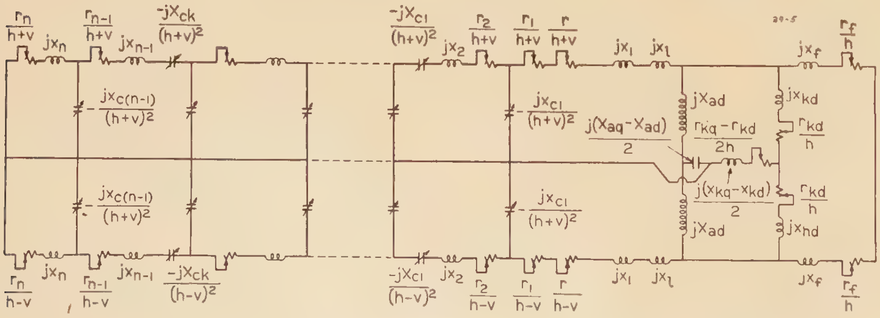

$\frac{r_n}{h+v}$ jx_n $\frac{r_{n-1}}{h+v}$ jx_{n-1} $\frac{-jX_{ck}}{(h+v)^2}$
$\frac{-jX_{c1}}{(h+v)^2}$ jx_2 $\frac{r_2}{h+v}$ $\frac{r_1}{h+v}$ $\frac{r}{h+v}$ jx_1 jx_l jx_f $\frac{r_f}{h}$
$\frac{jx_{c(n-1)}}{(h+v)^2}$ $\frac{jx_{c1}}{(h+v)^2}$ $\frac{j(X_{aq}-X_{ad})}{2}$ jX_{ad} jx_{kd} $\frac{r_{kq}-r_{kd}}{2h}$ $\frac{r_{kd}}{h}$ $\frac{j(x_{kq}-x_{kd})}{2}$ jx_{kd}
$\frac{r_n}{h-v}$ jx_n $\frac{r_{n-1}}{h-v}$ jx_{n-1} $\frac{-jX_{ck}}{(h-v)^2}$ $\frac{-jX_{c1}}{(h-v)^2}$ jx_2 $\frac{r_2}{h-v}$ $\frac{r_1}{h-v}$ $\frac{r}{h-v}$ jx_1 jx_l jx_f $\frac{r_f}{h}$

Figure 5. Self-excitation network of the transmission system (mass = infinite)

Spinor Form of the Equation of Motion

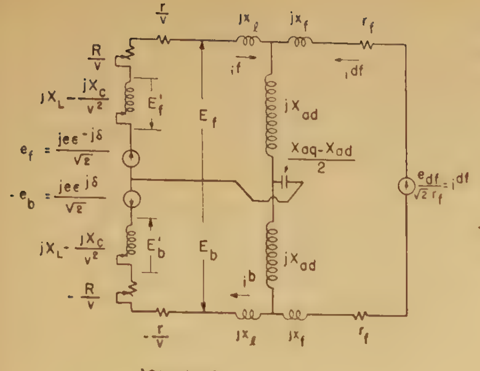
When the components of $\Gamma_{\alpha\beta,\gamma}$ contain complex numbers or complex functions, it has been shown[3] that eight different $\Gamma_{\alpha\beta,\gamma}$ may occur in the most general case, namely $\Gamma_{\alpha\beta,\bar{\gamma}}$, $\Gamma_{\alpha\bar{\beta},\gamma}$, $\Gamma_{\bar{\alpha},\beta,\gamma}$ $\Gamma_{\alpha\beta,\gamma}$ and their conjugates. It has been found by the writer that in the Lagrangean equation, or at least in electrical machinery, six of the above eight expressions are zero, and only the following two have nonzero components, $\Gamma_{\alpha\beta,\bar{\gamma}}$ and $\Gamma_{\bar{\alpha}\beta,\bar{\gamma}}$. In particular it was found, that of the two components of $\Gamma_{\alpha\beta,\gamma}$ namely

$$\Gamma_{\alpha\beta,\gamma} \overset{*}{=} [\alpha\beta,\bar{\gamma}] - 2S_{\bar{\gamma}\bar{\alpha}\beta} \qquad (3)$$

the nonholonomic Christoffel symbol $[\alpha\beta,\bar{\gamma}]$ has only its free index barred, while the torsion tensor $S_{\bar{\alpha}\bar{\beta}\bar{\gamma}}$ has its skew-symmetric indexes both barred. Hence in interchanging α and β in the latter, the components change signs only, but do not assume conjugate values.

From this property the following conclusions may be drawn, to be used as a laborsaving device:

In analyzing electrical machinery in complex variables (for instance when sym-


$\frac{r}{v}$
jx_ℓ
jx_f
r_f
$\frac{R}{v}$
$jX_L - \frac{jX_c}{v^2}$
E'_f
E_f
i^f
i^{df}
jX_{ad}
$e_f = \frac{jee^{-j\delta}}{\sqrt{2}}$
$\frac{X_{aq}-X_{ad}}{2}$
$-e_b = \frac{jee^{j\delta}}{\sqrt{2}}$
$\frac{e_{df}}{\sqrt{2}\,r_f} = i^{df}$
$jX_L - \frac{jX_c}{v^2}$
E'_b
E_b
i^b
jX_{ad}
$-\frac{R}{v}$
$-\frac{r}{v}$
jx_ℓ
jx_f
r_f

a) Steady-State Network

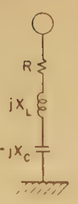
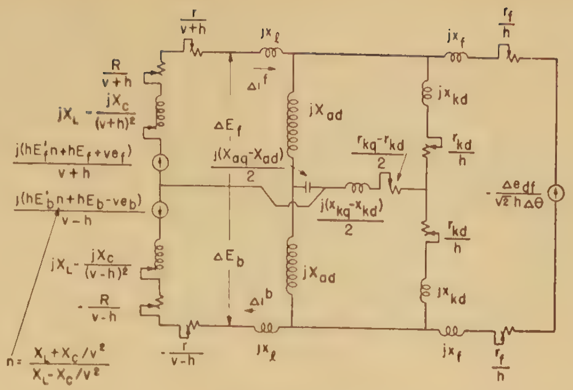


Figure 6. Salient-pole synchronous machine connected to infinite bus through *R-L-C* tie-line


$\frac{r}{v+h}$
jx_ℓ
jx_f
$\frac{r_f}{h}$
$\frac{R}{v+h}$
Δi^f
$jX_L - \frac{jX_c}{(v+h)^2}$
jX_{ad}
jx_{kd}
ΔE_f
$\frac{j(hE'_f n + hE_f + ve_f)}{v+h}$
$\frac{j(X_{aq}-X_{ad})}{2}$
$\frac{r_{kq}-r_{kd}}{2}$
$\frac{r_{kd}}{h}$
$-\frac{\Delta e_{df}}{\sqrt{2}\,h\,\Delta\theta}$
$\frac{j(hE'_b n + hE_b - ve_b)}{v-h}$
$\frac{j(x_{kq}-x_{kd})}{2}$
$\frac{r_{kd}}{h}$
ΔE_b
$jX_L - \frac{jX_c}{(v-h)^2}$
jX_{ad}
jx_{kd}
Δi^b
$-\frac{R}{v-h}$
$n = \frac{X_L + X_c/v^2}{X_L - X_c/v^2}$
$-\frac{r}{v-h}$
jx_ℓ
jx_f
$\frac{r_f}{h}$

b) Hunting Network

metrical components are introduced or when polyphase machines are studied) the usual tensor equations containing the unbarred $\Gamma_{\alpha\beta,\gamma}$ may be used in all manipulations, even though the variables are complex. However when $\Gamma_{\alpha\beta,\gamma}$ is replaced by its component objects, then $[\alpha\beta, \bar{\gamma}]$ should have its free index barred and $S_{\bar{\alpha}\bar{\beta}\gamma}$ its two skew-symmetric indexes.

Accordingly the equation of motion of Lagrange

$$e_\alpha = R_{\alpha\beta} i^\beta + a_{\alpha\beta}\frac{di^\beta}{dt} + \Gamma_{\beta\gamma,\alpha} i^\beta i^\gamma \tag{4}$$

may be written for complex variables as

$$\boxed{e_{\bar{\alpha}} = R_{\bar{\alpha}\beta} i^\beta + a_{\bar{\alpha}\beta}\frac{di^\beta}{dt} + [\beta\gamma, \bar{\alpha}] i^\beta i^\gamma - 2S_{\bar{\alpha}\bar{\beta}\gamma} i^{\bar{\beta}} i^\gamma} \tag{5}$$

where $[\beta\gamma, \bar{\alpha}]$ is the nonholonomic Christoffel symbol.

From this notation automatically follows for instance that in calculating the torque one of the currents must be changed to its conjugate. It should be remembered that:

1. $e_{\bar{\alpha}}$ is normally barred.
2. i^α is normally unbarred.
3. The metric tensor $a_{\bar{\alpha}\beta}$ is no longer symmetrical, but hermitian

$$a = a_t^* \text{ or } a_{\bar{\alpha}\beta} = a_{\beta\alpha} \tag{6}$$

4. When in the transient equations the $p = d/dt$ are replaced by $j\omega$, the spin indices do not refer to these new complex numbers. For that reason p should be replaced by $k\omega$ where k plays a similar role to j, still it remains distinct from it.
5. When the free index is a geometrical index s the equation represents torques. Hence only the real part of the torque equations is calculated.

The Equations of Hunting

Making small changes in the equation of motion of Lagrange (equation 4) the conventional nontensorial equation of hunting is

$$E_\alpha + \frac{\partial e_\alpha}{\partial x^\beta}\Delta x^\beta = \frac{\partial R_{\alpha\beta}}{\partial x^\gamma} i^\beta \Delta x^\gamma + R_{\alpha\beta}\Delta i^\beta + a_{\alpha\beta}\frac{d(\Delta i^\beta)}{dt} + \Gamma_{\beta\gamma,\alpha}\Delta i^\beta i^\gamma + \Gamma_{\beta\gamma,\alpha} i^\beta \Delta i^\gamma + \frac{\partial a_{\alpha\beta}}{\partial x^\gamma}\frac{di^\beta}{dt}\Delta x^\gamma + \frac{\partial \Gamma_{\beta\gamma,\alpha}}{\partial x^\delta} i^\beta i^\gamma \Delta x^\delta \tag{7}$$

where E_α is any small impressed force. In the special case when $a_{\alpha\beta}$ and $\Gamma_{\alpha\beta,\gamma}$ are constant, the last two terms disappear, and the equation becomes a tensor equation.

The tensorial equation of small oscillations and of their geometrical analogue, the disturbed geodesic is well known.[6–7] Its derivation, when applied to electrical machinery, has been given in reference 8, equation 161 as

$$\boxed{E_\alpha + \delta e_\alpha = R_{\alpha\beta}\delta i^\beta + a_{\alpha\beta}\frac{\delta(\delta i^\beta)}{dt} + K_{\delta\gamma\beta\alpha} i^\beta i^\delta dx^\gamma + R_{\alpha\beta\gamma} i^\beta dx^\gamma} \tag{8}$$

Each symbol in the equation is a tensor.

Replacing each tensor by its component geometric objects, the above equation may be written as (reference 8, equation 162):

$$\begin{array}{ll}
E_\alpha + \delta e_\alpha & \left\{ \left| E_\alpha + \dfrac{\partial e_\alpha}{\partial x^\beta}\Delta x^\beta + \Gamma_{\gamma\delta,\alpha} e^\gamma \Delta x^\delta \right.\right. \\
= \delta(R_{\alpha\beta} i^\beta) & \left\{ \left| = R_{\alpha\beta}\Delta i^\beta + \dfrac{\partial R_{\alpha\beta}}{\partial x^\gamma} i^\beta \Delta x^\gamma + \Gamma_{\gamma\delta,\alpha} R^{\gamma}_{\cdot\beta} i^\beta \Delta x^\delta \right.\right. \\
+a_{\alpha\beta}\dfrac{\delta(\delta i^\beta)}{dt} & \left\{ \left| \begin{array}{l} +\Gamma_{\gamma\delta,\alpha} e^\gamma \Delta x^\delta - \Gamma_{\gamma\delta,\alpha} R^{\gamma}_{\cdot\beta} i^\beta \Delta x^\delta + a_{\alpha\beta}\dfrac{d}{dt}\Delta i^\beta + \dfrac{\partial a_{\alpha\beta}}{\partial x^\gamma}\dfrac{di^\beta}{dt}\Delta x^\gamma + \Gamma_{\beta\gamma,\alpha}\Delta i^\beta i^\gamma \\ +\Gamma_{\beta\gamma,\alpha} i^\beta \Delta i^\gamma + \left(\dfrac{\partial \Gamma_{\beta\gamma,\alpha}}{\partial x^\delta} + \Gamma_{\lambda\delta,\alpha}\Gamma_{\beta\gamma}{}^\lambda - \Gamma_{\lambda\gamma,\alpha}\Gamma_{\beta\delta}{}^\lambda - 2\Gamma_{\beta\lambda,\alpha}\Omega_{\delta\gamma}{}^\lambda\right) i^\beta i^\delta \Delta x^\gamma \end{array} \right.\right. \\
+K_{\delta\gamma\beta\alpha} i^\beta i^\delta dx^\gamma & \left\{ \left| +\left(\dfrac{\partial \Gamma_{\beta\delta,\alpha}}{\partial x^\gamma} - \dfrac{\partial \Gamma_{\beta\gamma,\alpha}}{\partial x^\delta} + \Gamma_{\lambda\gamma,\alpha}\Gamma_{\beta\delta}{}^\lambda - \Gamma_{\lambda\delta,\alpha}\Gamma_{\beta\gamma}{}^\lambda + 2\Gamma_{\beta\lambda,\alpha}\Omega_{\delta\gamma}{}^\lambda\right) i^\beta i^\delta \Delta x^\gamma \right.\right.
\end{array} \tag{9}$$

The equation given in the reference is generalized here to include nonholonomic reference frames, as all examples considered below involve nonholonomic systems.

The Set to Be Added and Subtracted

The tensorial equation of hunting (9) consists of the standard equation of hunting (7) with a set of terms ΔE_α *added* and *subtracted*. The set is

$$\Delta E_\alpha = -\Gamma_{\beta\gamma,\alpha}(e^\beta - R^{\beta}_{\cdot\delta} i^\delta - \Gamma_{\epsilon\delta}{}^\beta i^\epsilon i^\delta)\Delta x^\gamma + \left(\Gamma_{\lambda\delta,\alpha}\Gamma_{\beta\gamma}{}^\lambda + \frac{\partial \Gamma_{\beta\gamma,\alpha}}{\partial x^\delta} + 2\Gamma_{\beta\lambda,\alpha}\Omega_{\delta\gamma}{}^\lambda\right) i^\beta i^\delta \Delta x^\gamma \tag{10}$$

This set may be simplified by noting that by the equation of motion (4)

$$e^\beta - R^{\beta}_{\cdot\delta} i^\delta - \Gamma_{\epsilon\delta}{}^\beta i^\epsilon i^\delta = \frac{di^\beta}{dt} = pi^\beta \tag{11}$$

Hence the set to be added and subtracted from the standard equations of hunting is

$$\boxed{\Delta E_\alpha = -\Gamma_{\beta\gamma,\alpha}\frac{di^\beta}{dt}\Delta x^\gamma + \left(\Gamma_{\lambda\delta,\alpha}\Gamma_{\beta\gamma}{}^\lambda + 2\Gamma_{\beta\lambda,\alpha}\Omega_{\delta\gamma}{}^\lambda + \frac{\partial \Gamma_{\beta\gamma,\alpha}}{\partial x^\delta}\right) i^\beta i^\delta \Delta x^\gamma} \tag{12}$$

It should be noted that in electrical machinery Δx^γ must always have a geometrical index s as Δx^s, since all objects are functions only of x^s. Also in all the examples considered here, the two machines *run at the same speed*, $x^s=\delta_2-\delta_1=\text{constant}$, hence $i^s=p(\theta_2-\theta_1)=0$, and the last term is zero.

By allowing the *free* index α to assume the electrical variables m or the geometrical variables s, the equation of hunting automatically separates into an equation of voltage and an equation of torque. The latter may be expressed in terms of geometric objects of valence two $\boldsymbol{G}$, $\boldsymbol{V}$, and so forth, instead of objects of valence three or four.

Tensor Form of the Voltages and Torques

1. If $\alpha=\overline{\boldsymbol{m}}$, the remaining terms may be expressed as

$$\begin{aligned}\Delta E_{\overline{\boldsymbol{m}}}&=-\Gamma_{\beta s\bar{m}}\frac{di^\beta}{dt}\Delta x^s+(\Gamma_{\lambda\delta,\ \bar{m}}\Gamma_{\beta s}{}^{\lambda}+\\ &\qquad 2\Gamma_{\beta\lambda,\ \bar{m}}\Omega_{\delta s}{}^{\lambda})i^\beta i^\delta\,\Delta x^s\\ &=-\Gamma_{ns,\ \bar{m}}pi^n\,\Delta x^s+2\Gamma_{\bar{s}n,\bar{m}}\Omega_{ks}{}^{n}i^{\bar{s}}i^k\,\Delta x^s+\\ &\qquad\Gamma_{ns,\ \bar{m}}\Gamma_{ks}{}^{n}i^k i^s\,\Delta x^s\\ &=-V_{\bar{m}n}pi^n\,\Delta\delta-G_{\bar{m}n}L^{n\bar{\tau}}V_{\bar{\tau}k}i^k p\theta\,\Delta\delta+\\ &\qquad V_{\bar{m}n}L^{n\bar{\tau}}V_{\bar{\tau}k}i^k p\delta\end{aligned}$$

$$\begin{aligned}\Delta\boldsymbol{E}=-\boldsymbol{V}\cdot p\boldsymbol{i}\,\Delta\delta-\boldsymbol{G}\cdot\boldsymbol{L}^{-1}\cdot\boldsymbol{V}\cdot\boldsymbol{i}p\theta\,\Delta\delta+\\ \boldsymbol{V}\cdot\boldsymbol{L}^{-1}\cdot\boldsymbol{V}\cdot\boldsymbol{i}p\delta\end{aligned}\qquad(13)$$

If the two machines run at the same speed $Vp\delta=0$, and the last term is zero.

Hence *the additional term to the equation of voltage is* (to be added and subtracted)

$$\boxed{\Delta\boldsymbol{E}=(-\boldsymbol{V}\cdot p\boldsymbol{i}-\boldsymbol{\gamma}_t{}^*\cdot\boldsymbol{V}p\theta\cdot\boldsymbol{i})\,\Delta\delta}\qquad(14)$$

where $\delta=\pm(\theta_2-\theta_1)$. It must be remembered that this formula is valid only if the two machines run at the same steady-state speed, $p\theta$.

2. If $\bar{\alpha}=\bar{\boldsymbol{s}}(\delta=n$ and $\beta=k)$

$$\Delta E_{\bar{s}}=(\Gamma_{mn,\ \bar{s}}\Gamma_{\bar{k}s}{}^{m}+2\Gamma_{\bar{k}m,\ \bar{s}}\Omega_{ns}{}^{m})i^{\bar{k}}i^n\,\Delta x^s\qquad(15)$$

The first term is zero and the second term is

$$\begin{aligned}2\Gamma_{\bar{k}m,\ \bar{s}}\Omega_{ns}{}^{m}i^{\bar{k}}i^n\,\Delta x^s=-i^{\bar{k}}G_{\bar{k}n}L^{m\tau}V_{\tau n}i^n\,\Delta\delta=\\ -\boldsymbol{i}\cdot\boldsymbol{G}\cdot\boldsymbol{L}^{-1}\cdot\boldsymbol{V}\cdot\boldsymbol{i}\,\Delta\delta\end{aligned}\qquad(16)$$

Hence *the additional term of the equation of torque is* (to be added and subtracted)

$$\boxed{\Delta T=-\boldsymbol{i}\cdot\boldsymbol{\gamma}_t{}^*\cdot\boldsymbol{V}\cdot\boldsymbol{i}\,\Delta\delta}\qquad(17)$$

Example of Two Instrument Selsyns

In interconnecting two slip-ring induction motors, Figure 1 (or synchronous machines) running at an angle $\delta=\theta_2-\theta_1$ from each other, their equivalent circuit requires two phase shifters. One with $\epsilon^{j\delta}$ in the $\boldsymbol{f}_r$ mesh, another with $\epsilon^{-j\delta}$ in the $\boldsymbol{b}_r$ mesh. One of these phase shifters may be eliminated by rotating first all the axes of the second machine by $\epsilon^{j\delta}$. Then the two machines are interconnected (if both are expressed along sequence axes) by

$$\boldsymbol{C}=\begin{array}{c|c|c|c|c|c|c|}
 & \boldsymbol{f}_1 & \boldsymbol{b}_1 & \boldsymbol{f}_r & \boldsymbol{b}_r & \boldsymbol{f}_2 & \boldsymbol{b}_2\\ \hline
\boldsymbol{f}_{s1} & 1 & & & & & \\
\boldsymbol{f}_{r1} & & & 1 & & & \\
\boldsymbol{b}_{s1} & & 1 & & & & \\
\boldsymbol{b}_{r1} & & & & 1 & & \\
\boldsymbol{f}_{s2} & & & & & 1 & \\
\boldsymbol{f}_{r2} & & & 1 & & & \\
\boldsymbol{b}_{s2} & & & & & & 1\\
\boldsymbol{b}_{r2} & & & & -\epsilon^{-2j(\theta_2-\theta_1)} & & \\ \hline
\end{array}\qquad(18)$$

The various tensors of the individual selsyns are given in reference 10. The resultant transient $\boldsymbol{Z}$ tensor of the interconnected system is found by

$$\boldsymbol{Z}'=\boldsymbol{C}_t{}^*\cdot\boldsymbol{Z}\cdot\boldsymbol{C}+\boldsymbol{C}_t{}^*p\boldsymbol{L}\cdot\boldsymbol{C}\qquad(19)$$

given in equation 21. The corresponding equivalent circuits are shown in Figure 2. The voltages $\Delta\boldsymbol{E}$ impressed on the hunting networks are given in equation 22. The torques are found by

$$\Delta T=\boldsymbol{i}\cdot\Delta\boldsymbol{E}^*+\Delta\boldsymbol{i}\cdot\boldsymbol{E}^*+\boldsymbol{i}^*\cdot\frac{\partial\boldsymbol{G}}{\partial\delta}\cdot\boldsymbol{i}\,\Delta\delta\qquad(20)$$

$$Z=\frac{1}{4}\times\begin{array}{c|c|c|c|c|c|c|}
 & f_1 & b_1 & f_r & b_r & f_2 & b_2\\ \hline
f_1 & r_{sd1}+r_{sq1}+(L_{sd1}+L_{sq1})p & r_{sd1}-r_{sq1}+(L_{sd1}-L_{sq1})p & (M_{d1}+M_{q1})p & (M_{d1}-M_{q1})p & & \\ \hline
b_1 & r_{sd1}-r_{sq1}+(L_{sd1}-L_{sq1})p & r_{sd1}+r_{sq1}+(L_{sd1}+L_{sq1})p & (M_{d1}-M_{q1})p & (M_{d1}+M_{q1})p & & \\ \hline
f_r & (M_{d1}+M_{q1})(p-jp\theta_1) & (M_{d1}-M_{q1})(p-jp\theta_1) & 2r_{r1}+(L_{rd1}+L_{rq1})\cdot(p-jp\theta_1)+2r_{r2}+(L_{rd2}+L_{rq2})\cdot(p-jp\theta_1) & (L_{rd1}-L_{rq1})(p-jp\theta_1)+(L_{rd2}-L_{rq2})\epsilon^{-2j\delta}[p-j(2p\theta_2-p\theta_1)] & -(M_{d2}+M_{q2})\cdot(p-jp\theta_1) & -(M_{d2}+M_{q2})\cdot(p-jp\theta_1)\\ \hline
b_r & (M_{d1}-M_{q1})(p+jp\theta_1) & (M_{d1}+M_{q1})(p+jp\theta_1) & (L_{rd1}-L_{rq1})\cdot(p+jp\theta_1)+\epsilon^{2j\delta}(L_{rd2}-L_{rq2})\cdot[p+j(2p\theta_2-p\theta_1)] & 2r_{r1}+(L_{rd1}+L_{rq1})\cdot(p+jp\theta_1)+2r_{r2}+(L_{rd2}+L_{rq2})\cdot(p+jp\theta_1) & -\epsilon^{2j\delta}(M_{d2}-M_{q2})[p+j(2p\theta_2-p\theta_1)] & -\epsilon^{2j\delta}(M_{d2}+M_{q2})\cdot[p+j(2p\theta_2-p\theta_1)]\\ \hline
f_2 & & & -(M_{d2}+M_{q2})\cdot[p+j(p\theta_2-p\theta_1)] & -(M_{d2}-M_{q2})\cdot\epsilon^{-2j\delta}[p-j(p\theta_2-p\theta_1)] & r_{sd2}+r_{sq2}+(L_{sd2}+L_{sq2})\cdot[p+j(p\theta_2-p\theta_1)] & r_{sd2}-r_{sq2}+(L_{sd2}-L_{sq2})\cdot[p+j(p\theta_2-p\theta_1)]\\ \hline
b_2 & & & -(M_{d2}-M_{q2})\cdot[p+j(p\theta_2-p\theta_1)] & -(M_{d2}+M_{q2})\epsilon^{-2j\delta}\cdot[p-j(p\theta_2-p\theta_1)] & r_{sd2}-r_{sq2}+(L_{sd2}-L_{sq2})[p+j(p\theta_2-p\theta_1)] & r_{sd2}+r_{sq2}+(L_{sd2}+L_{sq2})\cdot[p+j(p\theta_2-p\theta_1)]\\ \hline
\end{array}\qquad(21)$$

$$\Delta E=\frac{1}{4}\times\begin{array}{c|l|l} & \Delta\theta_2 & \Delta\theta_1 \\ \hline f_1 & & \\ \hline b_1 & & \\ \hline f_r & (L_{rd2}-L_{rq2})\epsilon^{-2j\delta}\{-2ji^{br}p-2j[p-j(2p\theta_2-p\theta_1)]i^{br}\} & 4E_{f1}p-j[(X_{rd2}+X_{rq2})i^{fr}-(X_{md2}+X_{mq2})i^{f2}-(X_{md2}-X_{mq2})i^{b2}]p+(L_{rd2}-L_{rq2})\epsilon^{-2j\delta}\{ji^{br}p+2j[p-j(2p\theta_2-p\theta_1)]i^{br}\} \\ \hline b_r & (L_{rd2}-L_{rq2})\epsilon^{2j\delta}\{2ji^{fr}p+2j[p+j(2p\theta_2-p\theta_1)]i^{fr}\}+(M_{d2}-M_{q2})\epsilon^{2j\delta}\{-2ji^{f2}p-2j[p+j(2p\theta_2-p\theta_1)]i^{f2}\}+(M_{d2}+M_{q2})\epsilon^{2j\delta}\{-2ji^{b2}p-2j[p+j(2p\theta_2-p\theta_1)]i^{b2}\} & 4E_{b1}p+j(X_{rd2}+X_{rq2})i^{br}p+(L_{rd2}-L_{rq2})\epsilon^{2j\delta}\{-ji^{fr}p-2j[p+j(2p\theta_2-p\theta_1)]i^{fr}\}+(M_{d2}-M_{q2})\epsilon^{2j\delta}\{ji^{f2}p+2j[p+j(2p\theta_2-p\theta_1)]i^{f2}\}+(M_{d2}+M_{q2})\epsilon^{2j\delta}\{ji^{b2}p+2j[p+j(2p\theta_2-p\theta_1)]i^{b2}\} \\ \hline f_2 & (M_{d2}-M_{q2})\epsilon^{-2j\delta}\{ji^{br}p+2j[p-j(p\theta_2-p\theta_1)]i^{br}\}+4je_{f2}\epsilon^{-j\delta}-j[(X_{md2}+X_{mq2})i^{fr}-(X_{sd2}+X_{sq2})i^{f2}-(X_{sd2}-X_{sq2})i^{b2}]p & (M_{d2}-M_{q2})\epsilon^{-2j\delta}\{-ji^{br}p-2j[p-j(p\theta_2-p\theta_1)]i^{br}\}-4je_{f2}\epsilon^{-j\delta}+j[(X_{md2}+X_{mq2})i^{fr}-(X_{sd2}+X_{sq2})i^{f2}-(X_{sd2}-X_{sq2})i^{b2}]p \\ \hline b_2 & (M_{d2}+M_{q2})\epsilon^{-2j\delta}\{ji^{br}p+2j[p-j(p\theta_2-p\theta_1)]i^{br}\}+4je_{b2}\epsilon^{-j\delta}-j[(X_{md2}-X_{mq2})i^{fr}-(X_{sd2}-X_{sq2})i^{f2}-(X_{sd2}+X_{sq2})i^{b2}]p & (M_{d2}+M_{q2})\epsilon^{-2j\delta}\{-ji^{br}p-2j[p-j(p\theta_2-p\theta_1)]i^{br}\}-4je_{b2}\epsilon^{-j\delta}+j[(X_{md2}-X_{mq2})i^{fr}-(X_{sd2}-X_{sq2})i^{f2}-(X_{sd2}+X_{sq2})i^{b2}]p \end{array} \qquad (22)$$

It should be noted that no matter how the expressions in ΔE are grouped, they cannot be measured on the steady-state network. However if the following matrix (equation 14) is added to ΔE

$$\frac{1}{4}\times\begin{array}{c|l|l} & \Delta\bar{\theta}_2 & \Delta\bar{\theta}_1 \\ \hline f_1 & & \\ \hline b_1 & & \\ \hline f_r & (L_{rd2}-L_{rq2})(p-jp\theta_2)\epsilon^{-2j\delta}2ji^{br}-(L_{rd2}-L_{rq2})(p-jp\theta_2)\epsilon^{-2j\delta}2ji^{br} & -(L_{rd2}-L_{rq2})(p-jp\theta_2)\epsilon^{-2j\delta}2ji^{br}+(L_{rd2}-L_{rq2})(p-jp\theta_2)\epsilon^{-2j\delta}2ji^{br} \\ \hline b_r & (L_{rd2}+L_{rq2})(p+jp\theta_2)2ji^{br}-(L_{rd2}+L_{rq2})(p+jp\theta_2)2ji^{br} & -(L_{rd2}+L_{rq2})(p+jp\theta_2)2ji^{br}+(L_{rd2}+L_{rq2})(p+jp\theta_2)2ji^{br} \\ \hline f_2 & -(M_{d2}-M_{q2})\epsilon^{-2j\delta}2jpi^{br}+(M_{d2}-M_{q2})\epsilon^{-2j\delta}2jpi^{br} & (M_{d2}-M_{q2})\epsilon^{-2j\delta}2jpi^{br}-(M_{d2}-M_{q2})\epsilon^{-2j\delta}2jpi^{br} \\ \hline b_2 & -(M_{d2}+M_{q2})\epsilon^{-2j\delta}2jpi^{br}+(M_{d2}+M_{q2})\epsilon^{-2j\delta}2jpi^{br} & (M_{d2}+M_{q2})\epsilon^{-2j\delta}2jpi^{br}-(M_{d2}+M_{q2})\epsilon^{-2j\delta}2jpi^{br} \end{array} \qquad (23)$$

then in the presence of these additional expressions the components of ΔE may be regrouped and expressed in terms of measurable voltages and currents as

$$\Delta E=\frac{1}{4}\times\begin{array}{c|l|l} & \Delta\theta_2 & \Delta\theta_1 \\ \hline f_1 & & \\ \hline b_1 & & \\ \hline f_r & -2j(X_{md2}-X_{mq2})\epsilon^{-2j\delta}(1+h-v)ji^{br} & 2j(X_{md2}-X_{mq2})\epsilon^{-2j\delta}(1+h-v)ji^{br}+4j(E_{f1}+E_{f2})h \\ \hline b_r & -j(X_{rd2}+X_{rq2})2ji^{br}(1+h+v)+8jE_{b2}(1+h+v) & j(X_{rd2}+X_{rq2})2ji^{br}(1+h+v)-8jE_{b2}(1+h+v)+4j(E_{b1}+E_{b2})h \\ \hline f_2 & 2j(X_{md2}-X_{mq2})\epsilon^{-2j\delta}(1+h)ji^{br}-4jE_{fs}h+4jE_{f2}\epsilon^{-j\delta} & -2j(X_{md2}-X_{mq2})\epsilon^{-2j\delta}(1+h)ji^{br}+4jE_{fs}h-4je_{f2}\epsilon^{-j\delta} \\ \hline b_2 & 2j(X_{md2}+X_{mq2})\epsilon^{-2j\delta}(1+h)ji^{br}+4jE_{bs}h+4je_{bs}\epsilon^{-j\delta} & -2j(X_{md2}+X_{mq2})\epsilon^{-2j\delta}(1+h)ji^{br}-4jE_{bs}h-4je_{b2}\epsilon^{-j\delta} \end{array} \qquad (24)$$

Similarly, it should be noted that in the torque equation 20 the last term cannot be measured off the hunting network. Hence, if to the torque equation the following matrix (equation 17)

$$-\gamma_t^*\cdot V\cdot i+\bar{\gamma}_t^*\cdot V\cdot i=\begin{array}{c|l|l} & \Delta\theta_2 & \Delta\theta_1 \\ \hline f_r & -j(X_{rd2}-X_{rq2})\epsilon^{-2j\delta}2ji^{br}+j(X_{rd2}-X_{rq2})\epsilon^{-2j\delta}2ji^{br} & j(X_{rd2}-X_{rq2})\epsilon^{-2j\delta}2ji^{br}-j(X_{rd2}-X_{rq2})\epsilon^{-2j\delta}2ji^{br} \\ \hline b_r & j(X_{rd2}+X_{rq2})2ji^{br}-j(X_{rd2}+X_{rq2})2ji^{br} & -j(X_{rd2}+X_{rq2})2ji^{br}+j(X_{rd2}+X_{rq2})2ji^{br} \end{array} \qquad (25)$$

is added, then the torque equation may be regrouped, expressed as

$$\Delta T=\boldsymbol{i}\cdot\Delta\boldsymbol{E}'^{*}+\Delta\boldsymbol{i}\cdot\boldsymbol{E}^{*} \qquad (26)$$

and measured off the hunting network as usual.

The corresponding equivalent circuits are given in Figure 2 and the torque measurements in Table I. There are two similar hunting networks for each case of hunting, one containing everywhere $+h$ the other $-h$.

The two shaded circles represent a phase shifter, one shifting the current, the other the voltage by an angle 2δ. In writing equations for the armature mesh b_r by tracing out the equivalent circuit, it has to be remembered that only one equation need be written since the current generators form only junction pairs.

It should be noted that the curvature tensor enters only in the establishment of the voltage generator $2jE_b$ and the current generator $2ji^b$ that appear at the terminals of the machines.

Synchronous Machines

The same method of reasoning applies when two salient-pole synchronous machines are interconnected. Their equivalent circuits are given in Figure 3 and the torque measurements in Table II.

When a synchronous machine is connected to a transmission line, two cases are possible:

1. The reference frame is rigidly connected to the field pole, and the frame takes part in the oscillation. This case was considered in reference 11. The disadvantage of the resulting equivalent circuit of hunting is that it requires as many generators as there are meshes in the transmission line.
2. The reference frame is rigidly connected to the infinite bus. Since the latter is not assumed to hunt, the transmission network is free of generators. They appear now in the synchronous-machine field meshes, whose number is however few. The equivalent circuits for this case are given in Figure 4 and the torque measurements in Table III. The two Figures 5 and 6 are reproduced from reference 11.

The results of a study made on Figures 4–6 are given in a companion paper.[12]

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Abnormal Overvoltages Caused by Transformer Magnetizing Currents in Long Transmission Lines

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As power-transmission distances have increased, new problems, both technical and economic have been encountered, analyzed, and taken into consideration in the design and operation of power systems. As transmission distances continue to increase it is logical to expect that in addition to the already exposed problems, new ones may arise which previously have not required consideration. It is the purpose of this paper to present an analysis of a technical problem which falls in the latter category.

In the course of studying various problems associated with long-transmission-line phenomena, it has been found by means of the transient analyzer that certain combinations of transmission-line length and system reactance can give rise to overvoltages of considerable magnitude when a lightly loaded transformer is connected to that transmission line. It is shown in the paper that such a system is most susceptible to overvoltage phenomena when the transformer is connected to the receiving end of that transmission line, although, theoretically, similar phenomena may occur for any transformer location including the transformer at the sending end. These overvoltages are in addition to the normal fundamental-frequency voltage rise and are of a combination subharmonic and harmonic nature. In magnitude, this overvoltage may be twice as high as that which can be calculated by conventional methods when transformer magnetizing currents are neglected.

A comprehensive analysis of representative circuits with and without series-capacitor compensation has been made to determine the regions of abnormal behavior and the effectiveness of various practical methods for safely limiting such overvoltages. Such practical means for controlling the overvoltage include proper switching and relaying arrangements, the synchronous condenser, shunt reactors, and resonant shunts.

The results presented include a physical picture of the underlying mechanism which gives rise to the phenomenon. It is felt that these results will be of considerable importance from the standpoint of designing and operating systems involving the transmission of power over distances in the practical range above 200 miles.

Summary and Conclusions

The phenomena discussed in this paper are more likely to occur when the transformer is located at the receiving end and is lightly loaded or carrying no load. While an unloaded or lightly loaded transformer located at any point along a transmission line can give rise to these overvoltage phenomena, the system is most susceptible to such disturbances when the transformer is at the receiving end of the line because of the normal frequency rise in voltage with increasing distance from the source end. It should be borne in mind that either end of a long transmission line may become the receiving end in the sense employed in this study. The condition would probably be most serious when accompanied by generator overspeeding immediately following sudden loss of load caused by low side switching, which would simultaneously remove any receiving-end synchronous-condenser capacity otherwise normally present.

In designing a power system requiring transmission distances in the practical range above 200 miles, consideration should be given to the possibility of overvoltages such as these discussed in the paper. The more important factors the effects of which should be evaluated are

1. System frequency (including range due to generator overspeeding).
2. System reactance.
3. Line length and losses.
4. Receiving-end transformer size and saturation characteristics.
5. Switching arrangement
   (*a*). Use of high side breakers, with proper relaying, may prevent unloaded transformers from every being on the receiving end of the line.
   (*b*). If no high side breakers are used, care should be taken so that the receiving-end synchronous-condenser capacity is operated as an integral part of the line and receiving-end transformer.
6. Series capacitors.

Remedial measures which are effective in eliminating the overvoltages for cases falling in the danger region include the following:

1. Switching arrangements (see 5 above).
2. Synchronous condensers.
3. Shunt reactors.
4. Second harmonic shunts.

Shunt reactors or the second harmonic shunt should be an integral part of the line, preferably located at or near the terminals of the receiving-end transformer for maximum effectiveness. They need not be on the high-voltage side of the transformer. To make the second harmonic shunt effective over a range of frequencies likely to be encountered as a result of possible overspeeding, it would be preferable to have it tuned for a frequency slightly higher than the second harmonic. However, with series capacitors, the second harmonic shunt alone is not capable of stabilizing the voltage at the receiver end.

In some specific cases it may be desirable to set up the system in miniature to

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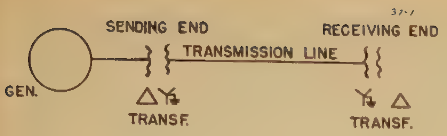


Figure 1. One-line diagram of system studied

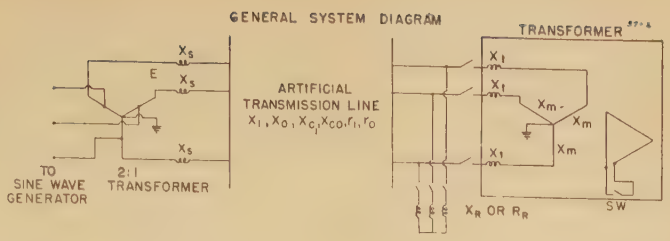


Figure 2. Three-phase miniature system equivalent of the system shown in Figure 1

evaluate accurately the effects of all of the factors involved under various conditions of operation.

System Studied

Figure 1 shows the basic circuit analyzed. In a system such as this, the receiving-end transformer would become considerably overexcited if its load were suddenly lost during a system disturbance severe enough to cause the receiving-end low-voltage circuit breakers to open. This would leave the transformer energized with no load and thus with only exciting current flowing. However, the magnetizing impedance of a transformer, while fundamentally reactive, is a variable quantity because of saturation of the iron core. During periods of saturation, relatively high peak currents may flow momentarily.

Analysis of the problem involving a distributed constant line, and a saturable reactance would be extremely complicated and time-consuming using ordinary methods. Therefore the transient-analyzer method of miniature-system representation, which is ideally suited to the study of such nonlinear circuit phenomena,[2,10,11] was used. For the purpose of analysis by this method, the system of Figure 1 was set up as shown in Figure 2. The artificial transmission line and the miniature transformers used at the receiving end have been described previously,[1,2] and so no space will be devoted to a description of the miniature system in this paper. It will be sufficient to point out that the abruptness of transformer saturation could be varied simply by varying X_t as shown in Figure 3. The values of X_t used were always small relative to X_m so that X_m remained substantially independent of X_t/X_m. Consequently the connected receiving transformer kilovolt-amperes remained essentially independent of X_t/X_m, and only the saturation characteristic of the receiving-end transformer was modified by varying X_t/X_m as shown in Figure 3. Each single-phase transformer used at the receiving end had a magnetizing impedance of $X_m=13{,}000$ ohms at normal voltage of 110 volts. Two of these units could be put in each phase in parallel, thus halving the value of X_m, or two units could be put in series in each phase (provided values of E *in volts* were doubled) thus doubling the value of X_m. In any case, the receiving-end transformer kilovolt-amperes to be associated with any value of X_m is given by the following relation.

$$X_m=\frac{\text{kv}^2\times 1{,}000}{\text{kva}\times I_m}$$

where

X_m is the receiving-end transformer magnetizing reactance in ohms per phase at normal voltage

Kv is normal system line-to-line voltage

Kva is the receiving-end transformer-bank rating

I_m is the exciting current in per unit per phase at normal voltage.

Thus, if $X_m=13{,}000$ ohms, and if a three per cent exciting current and a 230-kv system are assumed, the receiver-end transformer kilovolt-ampere rating represented by the miniature system would be

$$\text{kva}=\frac{(230)^2(1{,}000)}{13{,}000(0.03)}=135{,}000\text{ kva}$$

Provision was made for operating the receiving-end transformer with or without the delta open. Shunt reactors or resistors of variable kilovolt-ampere rating, as well as resonant shunts and synchronous condensers, could be switched in at the receiving end. Series capacitors for line-reactance compensation could be inserted as desired. Provision was made for varying the resistance of the transmission line and of the receiving-end transformer and for varying the sine-wave generator voltage, thus changing the operating flux density in the receiving-end transformer as desired. Consequently numerous factors separately or in combination could be carefully studied.

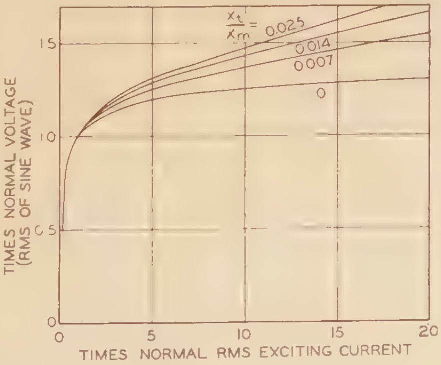


Figure 3. Saturation curves used in this study

D-c resistance of winding expressed as a ratio to X_m is 0.0003. $X_m=13{,}000$ ohms at 110 volts, 60 cycles, for one transformer

The effect of lightning arresters in limiting these overvoltages and thus protecting terminal equipment was not included in this study. It follows from the results revealing the nature of the phenomenon, however, that lightning arresters may be damaged by such sustained overvoltages.

Nomenclature

E = voltage back of X_s in per unit

X_s = source reactance

X_t = transformer series reactance (for saturation adjustment)

$x_0/x_1=\dfrac{\text{zero-sequence line reactance}}{\text{positive-sequence line reactance}}$

$r_1/x_1=\dfrac{\text{line resistance}}{\text{line reactance}}$ (positive sequence)

$V_R=\dfrac{\text{voltage at receiving end with transformer}}{\text{voltage at receiving end without transformer}}$

$X_{c1}/X_R=\dfrac{\text{(positive-sequence capacitive reactance)}}{\text{shunt reactance}}$

$r_0/x_0=\dfrac{\text{line resistance}}{\text{line reactance}}$ (zero sequence)

f = cycles per second

X_m = magnetizing reactance in ohms at normal voltage and frequency

X_{c1}, X_{co} = positive- and zero-sequence shunt capacitive reactance of line respectively

X_{sc} = series-capacitor reactance

X_L = nonsaturable reactance in parallel with X_{sc}

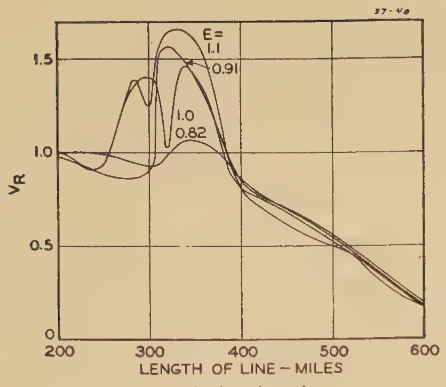
R = resistance in parallel with X_{sc}

X_R = nonsaturable shunt reactance at receiving-end transformer terminals

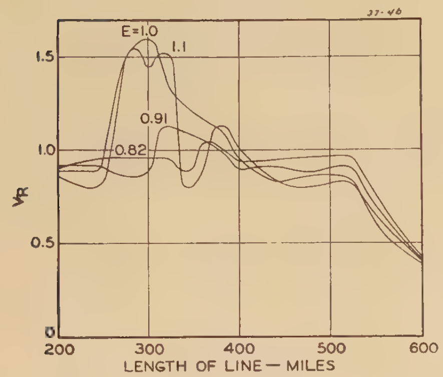
R_R = shunt resistors at receiving-end transformer terminals

Discussion of Results

With the system set up as in Figure 2, curves as shown in Figures 4a and 4b were obtained. It should be emphasized that the voltage V_R is a ratio of crest voltage to ground at the receiver end with the transformer connected, to the crest voltage at the same location with the transformer disconnected. Thus the normal rise in the line is eliminated from



(a). Delta closed



(b). Delta open

Figure 4. Voltages V_R obtained in the system of Figure 2

$X_s=86$ ohms
$f=60$ cycles per second
$x_0/x_1=4.0$
$r_1/x_1=0.1$
$X_t/X_m=0.014$
$X_m=13{,}000$ ohms
No series-capacitor compensation
No load on system
No shunt reactors

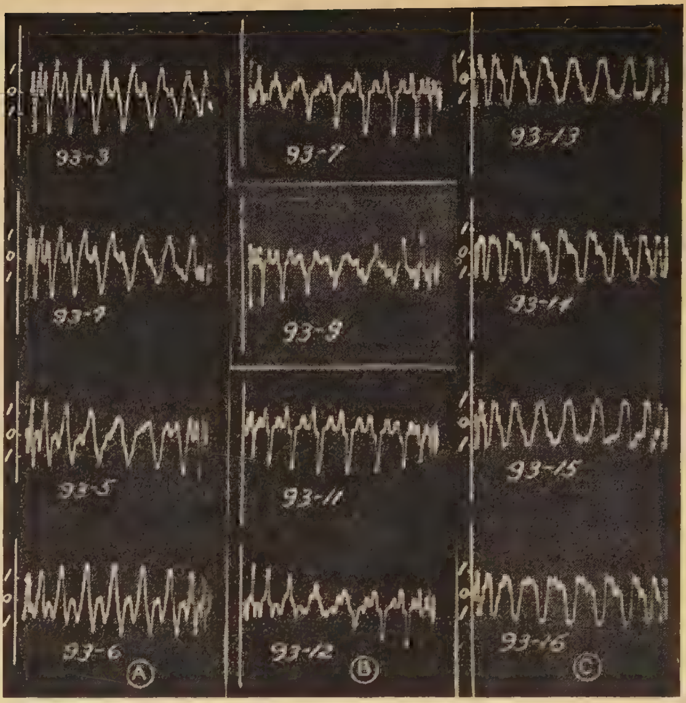
Figure 5. Oscillograms showing the nature of the phenomenon

System constants as in Figure 4a, with 320 miles of line

A. V_R—Phase-A voltage to ground at receiving-end transformer terminals

B. Receiving-end phase-A transformer magnetizing current

C. Voltage to ground at sending end times that which would exist without the transformer



the results, and the effect of the transformer alone is shown by these figures.

The oscillograms shown in Figure 5 are typical illustrations. No two of these oscillograms were taken simultaneously, so it is not possible to correlate instantaneous values of voltage and current. However, the variation in voltage and current wave shapes with time is indicated. The voltage is seen to be made up largely of a second harmonic and subharmonics. The current traces clearly show evidence of subharmonics and even harmonics. The time required for this condition to be reached following sudden loss of load varies from a couple of cycles to a second or two depending upon the value of E and the system constants.

The values of E maintained back of X_s range from a maximum of 1.1 down to values such that the effect of the transformer is negligible from the standpoint of producing overvoltages. It is felt that this covers the practical range, including the case of sudden loss of load at the receiving end. Unit voltage $E=1.0$ defines the normal operating point on the saturation curves of Figure 3 when applied directly to the terminals of the transformer.

It is to be noted that if E is reduced approximately to values such that the receiving-end transformer is not overexcited, no overvoltages will occur. It is of significance also that the region of abnormality is not greatly affected by the delta being opened or closed. Furthermore, the phenomenon is essentially independent of the method of grounding the receiving-end transformer neutral when a closed delta winding is present.

It should be noted that, while the system studied is characterized by a "sending end" and a "receiving end," in the actual case, either end may become the receiving end in the sense employed here. That is, during a system disturbance, either end of the line may be switched out on the low-voltage side, leaving an unloaded system of the type illustrated in Figure 2.

Similar tests made under numerous miniature-system conditions have made it possible to summarize the effect of source reactance X_s as shown in Figure 6. Since the case of the delta open is of little practical significance, only the delta-closed condition is shown. This figure shows the regions in which V_R is greater than unity in terms of miles of transmission line and source reactance in ohms. Since the delta being opened has little effect on the region of abnormality in general, it is to be concluded that the reactance of the tertiary winding is of little consequence. Furthermore the negligible effect of the delta being opened has some significance from the standpoint of the theory involved, as will be shown later.

The curves of Figure 6 indicate that overvoltages are to be expected over a range of line lengths from 200 miles on up to slightly over 400 miles when the system frequency is 60 cycles per second, and source reactances are in the practical range. With lower frequencies the line lengths could be increased inversely as the frequency before encountering the difficulty. The maximum value of V_R obtained was 1.8 with the delta closed, and the length of line producing the maximum overvoltage is shown to increase as the source reactance decreases. Thus, under system conditions corresponding to these, voltages on the order of twice those normally expected may exist at the terminals of the receiving-end transformer if the line length and system source reactance are in the right combination, and if corona and lightning arresters are not voltage-limiting factors.

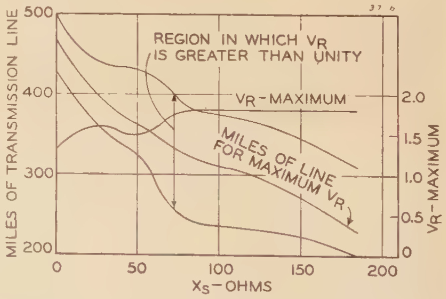


Figure 6. Boundaries of overvoltage region as affected by line length and source reactance with receiving-end transformer delta closed

Frequency 60 cycles per second

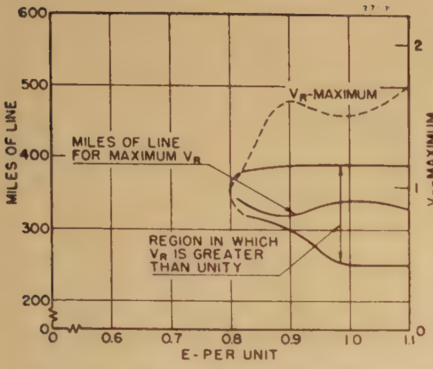


Figure 7. Effect of sending-end voltage

System constants as in Figure 4a

While the curves of Figure 6 are for the transformer located at the receiving end of the transmission line, it can be shown (see Appendix II) that the critical combination of length of line and source reactance which is susceptible to these phenomena is independent of the transformer location. Even the sending-end transformer can produce the same phenomena for the same range of constants shown in Figure 6. However, a somewhat higher range of values of E (voltage back of equivalent system reactance, X_s) would be required to give rise to the phenomenon in this case. The receiving-end transformer location renders a given system more vulnerable to such disturbances because of the normal-frequency voltage rise in the line.

The effect of the voltage E maintained within the system is shown in Figure 7 for the same case as in Figure 4. As the system voltage is decreased, the range of system constants capable of giving rise to this abnormality decreases, and at $E=0.8$ the phenomenon disappears entirely. This suggests the theoretical possibility of avoiding such overvoltage phenomena by using low-flux density transformers, although other means may generally be more desirable.

The effect of varying the magnetizing reactance X_m is illustrated in Figure 8. It is believed that this covers the practical range of X_m and shows that the phenomenon is not critical to a particular value of X_m. Obviously if X_m is ∞ the value of $V_R=1$ as shown, and if very high values of X_m are connected to the transmission line, little disturbance of the type under discussion would be expected. In order to make the results more general, values of X_m are given in ohms, but they can readily be converted to specific transformer kilovolt-ampere ratings in systems of given voltage rating, if the magnetizing current in per cent is known or assumed. The formula given in the preceding section can be used for this purpose.

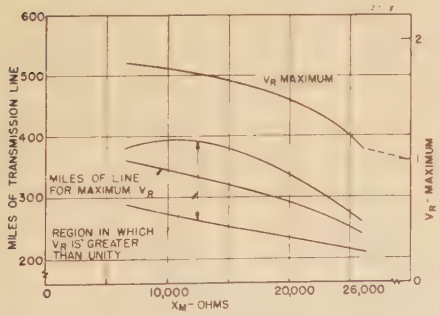


Figure 8. Effect of varying the receiving-end transformer magnetizing reactance X_m

System constants as in Figure 4a

The effect of line resistance is illustrated in Figure 9. It appears that, if r_1/x_1 could be as high as 0.4 or higher, the phenomena would not occur. However, practical values of r_1/x_1 in lines such as these under discussion would fall considerably below this value—on the order of 0.2 or less. Therefore line resistance would not be expected to eliminate the overvoltages in a practical case. Furthermore, any attempt to increase the circuit resistance may result in an increased tendency for hunting to take place.[3,4,12]

The effect of steepness of saturation is illustrated in Figure 10. It is evident from this illustration that the phenomenon is not critical to the abruptness of saturation over a practical range, although the more abrupt saturation characteristic in general appears to lead to higher voltages and broaden the range of constants for which they may be obtained.

The effect of generator overspeeding is illustrated in Figure 11. A system normally not in the region of overvoltages may be brought into the region by generator overspeeding as a result of loss of load.

After observing the effects of various factors in producing these overvoltages, means were investigated for controlling them. Among the first means investigated was shunt reactors placed at the terminals of the receiving-end transformer. The ability of reactors to accomplish the desired purpose is illustrated in Figure 12. It is interesting to note that shunt reactor kilovolt-amperes as high as 65 per cent of normal line-charging kilovolt-amperes may be required to keep V_R down to unity. This amount of reactance does not include compensation for the normal rise in the line since V_R as defined is independent of the normal rise in voltage in the line. If generator overspeeding is an appreciable factor, shunt reactors to take care of the normal fundamental-frequency rise in the line may not prevent the occurrence of this phenomenon because of the increase in applied frequency.

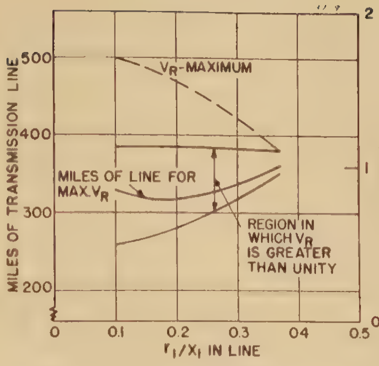


Figure 9. Effect of line resistance

System constants as in Figure 4a

Figure 13 indicates that if the receiving-end transformer saturates more abruptly, more reactor kilovolt-amperes may be required to stabilize the voltages

Figure 14 suggests that the maximum required X_c/X_R is likely to increase as the value of X_m decreases. This is representative for the practical range of X_m.

Figure 15 shows that a synchronous condenser is very effective in eliminating the overvoltage hazard. In most actual cases, a condenser at the receiver end of a size greater than the minimum required for overvoltage stability would likely be present. Consequently this is one very practical and effective means of overcoming the difficulty. However, provision must be made for keeping the condenser on the system, even though the load is lost. Thus, in addition to controlling the fundamental-frequency voltage at the receiver end during normal load conditions, a condenser is expected to be effective in eliminating abnormal harmonic and subharmonic voltages such as these during system disturbances.

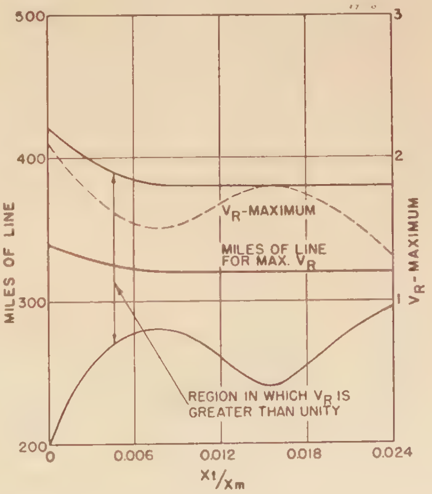


Figure 10. Effect of abruptness of transformer saturation at receiving end

System constants as in Figure 4a

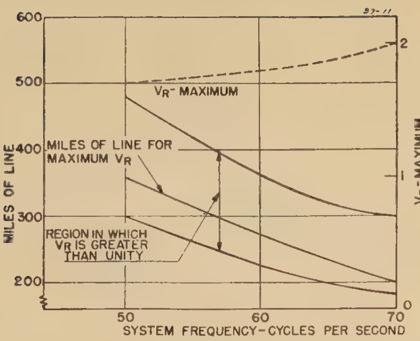


Figure 11. **Effect of generator overspeeding**

System constants as in Figure 4a

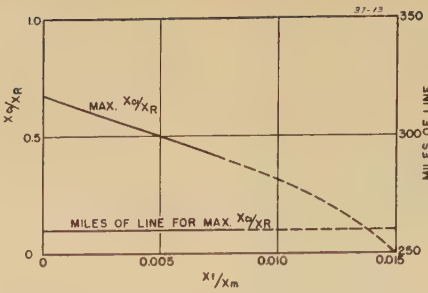


Figure 13. **Effect of abruptness of saturation on the required shunt reactor kilovolt-amperes to reduce V_R to unity**

System constants as in Figure 4a

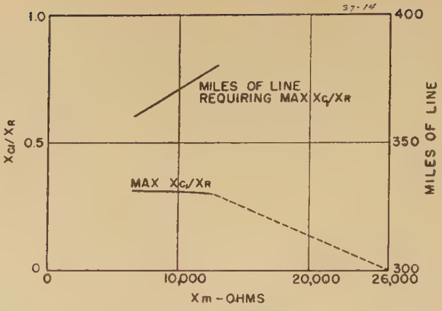


Figure 14. **Effect of magnitude of X_m on the required shunt reactor kilovolt-amperes to reduce V_R to unity**

System constants as in Figure 4a

The curves of Figure 15 require some discussion, because they show a minimum condenser requirement for the length of line giving rise to the maximum values of V_R. Practically, however, the necessity for reducing V_R at all is greatest when V_R is greatest. Therefore, for practical purposes, the maximum condenser requirements for this given case are more nearly the minimum values shown in Figure 15. In other words, a condenser of 15,000 kva would probably be adequate for keeping V_R from becoming destructive in magnitude for this case.

One other very effective means that has been found for eliminating the overvoltages is the resonant shunt located at the receiver-end transformer. The shunt may be either delta- or wye-connected. If wye-connected, it makes no difference whether the neutral point is grounded or not. Its effectiveness is illustrated in Figure 16. The reason for the effectiveness of the second harmonic shunt is discussed in the following section.

The effect of series capacitors at the mid-point of the transmission line is illustrated in Figure 17. Series capacitors at the mid-point of the line for 50 per cent compensation of the line drop were used for this case. The region of abnormality was broadened considerably by the presence of the series capacitors as can readily be seen by comparing this figure with Figure 4a or 4b. The reason for this broadening of the region of abnormality is that an additional phenomenon, different in nature, is introduced by the presence of series capacitors.[4] This is discussed further in the section headed "Theory."

It is possible to eliminate the phenomenon entirely by using shunt reactors located at the terminals of the receiving-end transformer. The amount of reactance required is shown for one case in Figure 18. In this figure also is shown the amount of shunt resistance required for the same case.

The possibility of using a resistor in shunt with the series capacitor has previously been given some consideration.[4] A reactor in parallel with the series capacitor also is effective as shown in Figure 19, although for this case, the effectiveness of either a resistor or reactor so placed decreases rapidly for lengths of line above 280 miles.

In Figure 20, the curves show the amount of reactance or resistance required in parallel with the series capacitor for the same case, the only difference being that a second-harmonic resonant shunt has been connected to the terminals of the receiving-end transformer. Reactance is seen to be quite effective as a means of eliminating the phenomenon in this case for line lengths up to well over 400 miles, while resistance is not nearly so effective. Furthermore, resistance is undesirable since it introduces losses during normal load conditions which in the proportions required would be intolerable in some cases.

A condenser of practical size at the receiving end is very effective in eliminating the voltage instability, either with or without series capacitors.

Theory

The cause of the phenomena discussed in the preceding section can best be understood by first considering a single-phase circuit as shown in Figure 21. Here the essential elements of the three-phase circuit of Figure 2 are present, the transmission line being represented by a capacitor, X_c, and the source reactance by X_L. The transformer X_m which can be connected or disconnected by the switch, corresponds to the receiving-end transformer of Figure 2 where X_t and X_m define the transformer characteristics.

With V_R defined as in the preceding discussion the results obtained by varying X_c/X_L and X_m/X_L are shown in Figure 22. Here two peaks of overvoltage are

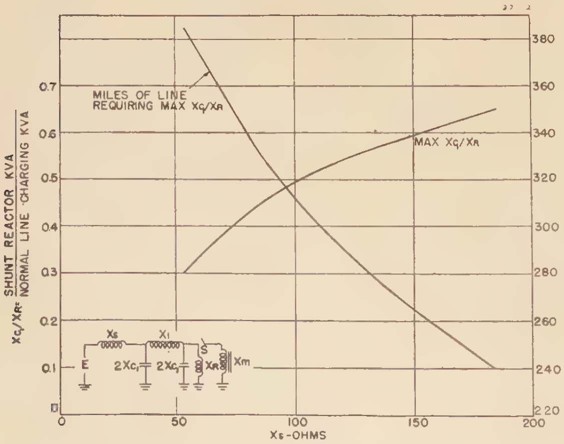


Figure 12. **Maximum shunt reactance kilovolt-amperes at receiving-end transformer terminals required to reduce V_R to unity**

Shunt-reactor kilovolt-amperes expressed as a ratio to normal line-charging kilovolt-amperes in accordance with the insert. System constants as in Figure 4a

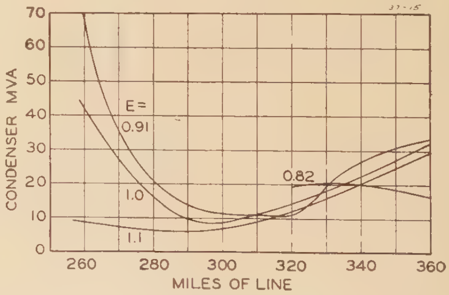


Figure 15. **Synchronous-condenser capacity required at the receiving end to reduce V_R to unity**

System as in Figure 4a, with $X_t/X_m=0$, transient reactance of condenser assumed to be 30 per cent on rating in 230-kv system. Voltage back of transient reactance of condenser, 133 per cent

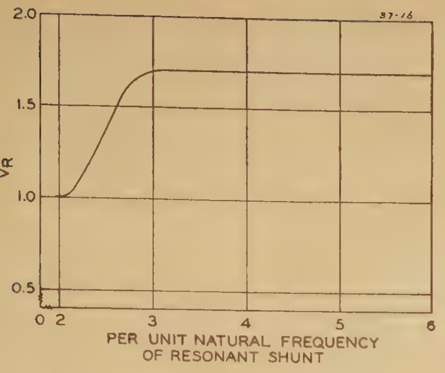


Figure 16. Effect of resonant shunt on V_R

Shunt consists of reactor and capacitor in series located at the terminals of the receiving-end transformer. Reactor 6.0 henrys. Capacitance varied. System constants as in Figure 4a with 320 miles of line

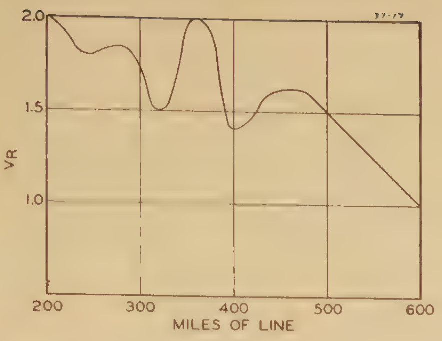


Figure 17. Relationship between V_R and miles of line with series capacitors

Series-capacitor kilovolt-amperes at mid-point of line correcting for 50 per cent of line reactance drop. System constants as in Figure 4a. $E \leqq 1.1$

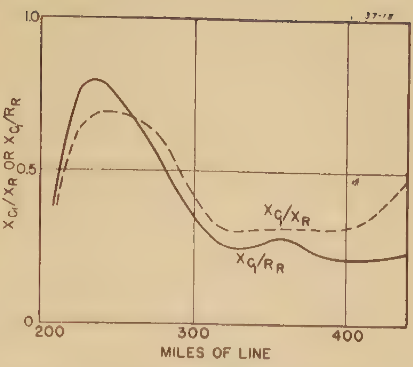


Figure 18. Shunt reactance or resistance required at the receiving-end transformer terminals to hold V_R down to unity for $E \leqq 1.1$

System constants as in Figure 4a with 50 per cent series-capacitor line-reactance drop compensation at the mid-point of the line

shown, one largely of second-harmonic and subharmonic nature for X_c/X_L just below 4 and the other largely of third-harmonic nature for X_c/X_L just below 9. The higher values of X_m/X_L cause the peaks of overvoltage to move closer to 4 and 9 respectively. Now when $X_c/X_L = 4$, the circuit viewed from the switch location with the switch open is tuned to the second harmonic, and when $X_c/X_L = 9$, it is tuned to the third harmonic.

For the case of X_c/X_L just below 9, it is not difficult to see why overvoltages will occur. If $X_c/X_L = 9$, the impedance of the circuit viewed from the switch with the switch open is infinite to the third harmonic neglecting resistance. Consequently, in trying to supply the required third to the transformer, a large third-harmonic drop is encountered, and this shows up at the transformer terminals as indicated in the curves. If X_c/X_L is lower than about 7.5 and higher than about 12, the impedance to the third harmonic required by the transformer is low enough essentially to eliminate the overvoltages of the third-harmonic type.

It is to be noted that the overvoltages of this type are a maximum when X_c/X_L is slightly less than 9. This means that the system impedance is capacitive and of a high value to the third harmonic. This is to be expected since, with the switch closed, the path for third harmonics includes X_m also, which, while difficult to evaluate under these saturated conditions, nevertheless is fundamentally reactive to the third and probably compensates for the resultant capacitive reactance of the system to bring the overall system more nearly in tune to the third harmonic at the point giving rise to the maximum V_R shown in Figure 22.

With regard to the peak values of V_R for X_c/X_L slightly less than 4, the explanation is more difficult, because, while the system impedance with the switch open is infinite (neglecting resistance) to the second harmonic when $X_c/X_L = 4$, the transformer in normal steady-state operation with sinusoidal voltage applied does not require a second harmonic to flow. Consequently the preceding explanation, while satisfactory for the region of third-harmonic overvoltages is not satisfactory for the region of second-harmonic overvoltages.

In order to visualize the cause of the second-harmonic overvoltages, it is desirable to substitute the equivalent circuit of Figure 23 for the circuit of Figure 21. In this figure, the two reactances X_L and X_c correspond to the reactances so labelled in Figure 21. The reactances X_m and X_s are constant (not saturable) and represent the transformer of Figure 21.

In this equivalent circuit, the saturation of the transformer is represented by two straight lines as shown in Figure 23b. The effect of saturation of this type is introduced in the equivalent circuit by closing switch S at every instant that the current i into the equivalent transformer exceeds a critical value and opening it at every instant that the current i falls below this critical value. In the illustration of Figure 23b, this critical value has been arbitrarily taken to be 0.9 of the crest value which would be taken by X_m if the switch S were never closed. On this basis the switch S would be opened and closed every half-cycle of fundamental frequency, or the switching cycle would be repeated at a frequency corresponding essentially to second-harmonic frequency, and, since the circuit made up of X_c and X_L is approximately tuned to the second harmonic, it is to be expected that the transient oscillations will be those corresponding at least approximately to the natural frequency as dictated by X_c/X_L.

Furthermore, this concept aids greatly in visualizing the production of subharmonics as well as the second harmonics. Consider the equivalent circuit as operating in a steady-state condition with switch S open. Then assume that on some half-cycle, saturation is to be introduced in accordance with the preceding theory. This will mean that switch S must be closed when i reaches 0.9 of the crest value it had been reaching on previous half-cycles. This instant can also be defined with respect to the fundamental-frequency voltage wave. Following the closing of the switch, a high peak of current will flow momentarily, and, when the total i returns to 0.9, switch S will be opened. This will leave a natural frequency oscillation in the circuit, and, when the current i has gone through zero and reaches a value of 0.9 again, switch S

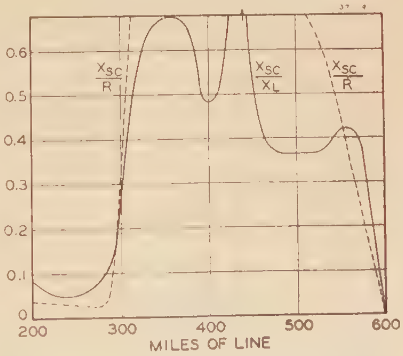


Figure 19. Resistance or reactance required in parallel with series capacitor to hold V_R down to unity for $E \leqq 1.1$

X_{sc} = reactance of series capacitors
X_L = reactance in parallel with X_{sc}
R = resistance in parallel with X_{sc}

Other system constants as in Figure 4a

50 per cent series-capacitor line-reactance drop compensation at the mid-point of the line

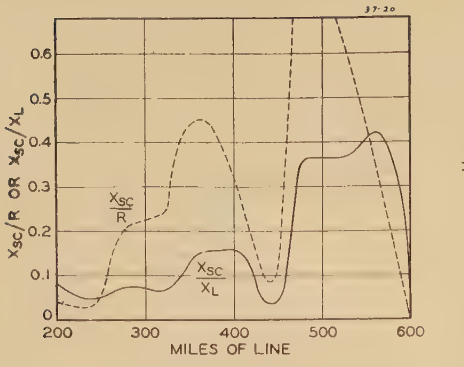


Figure 20. Resistance or reactance required in parallel with series capacitors to hold V_R down to unity for $E \leq 1.1$

X_{sc} = reactance of series capacitors
X_L = reactance in parallel with X_{sc}
R = resistance in parallel with X_{sc}

Second harmonic shunt at receiving-end transformer terminals. Shunt made up of 2,000 ω inductance in series with 0.32-microfarad capacitance wye-connected and ungrounded. Other system constants as in Figure 4a. 50 per cent series-capacitor line-reactance drop compensation at the mid-point of the line

must be closed. This time, however, the instant of closing is not the same with respect to the fundamental-frequency voltage wave as it was on the first half-cycle because of the transient condition. However, the switch must be opened again when the current decreases to 0.9 in accordance with our criterion. Furthermore, the next half-cycle will likely require that switch S be closed at still a different point on the voltage wave so that several cycles may elapse before two cycles are identical or nearly identical. Consequently subharmonics must appear, and the lowest subharmonic will be that corresponding to the period in cycles of fundamental frequency required for the exact initial conditions of closing switch S to repeat. It is conceivable that in a very low loss circuit, this might take a very long time, that is, the subharmonic frequency might be very low.

The preceding analysis of the single-phase circuit can be applied directly to the three-phase case by recognizing that the regions of abnormality in the single-phase circuit were associated with circuit constants which offered infinite impedance to the second or third harmonics. In Figure 24, the relationship between miles of line and source reactance for which infinite impedance is offered to harmonics is shown. Also the region in which V_R was found to be greater than unity (from Figure 6) is shown. It is significant that this region is close to the curve giving the relationship between miles of line and source reactance which offers infinite impedance to the second harmonic. Thus it corresponds to the single-phase case which for X_c/X_L slightly less than 4 gave rise to overvoltages of a similar nature. Furthermore, the maximum values of overvoltage in the single-phase case occurred when X_c/X_L was slightly less than 4. This meant that the impedance of the system was actually capacitive. In Figure 24, the region of abnormality lies above the line defining infinite impedance to the second harmonic. This also means that the reactance in this region is capacitive, thus further establishing the similarity between the three-phase and the single-phase case.

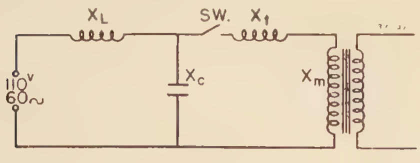


Figure 21. Single-phase lumped-constant circuit studied

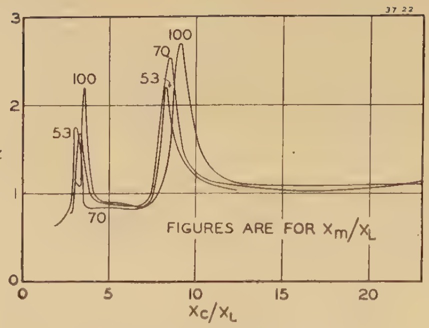


Figure 22. Results obtained in the study of the circuit shown in Figure 21

In Figure 24, a curve is shown for infinite impedance to the third harmonic. Infinite impedance to the third harmonic occurs naturally for shorter line lengths than for the second harmonic for a given source reactance. However, with a receiving-end transformer having a closed delta, the third harmonic is not necessarily supplied from the line, and therefore the system impedance has little effect. If the receiving-end transformer had no closed delta, then a region of overvoltages should be expected here of third-harmonic nature corresponding to the case of X_c/X_L slightly less than 9.0 in Figure 22. This region does exist but is considered of little practical significance in connection with this paper and so is not included in the curves.

The question as to why the delta being opened or closed made little difference in the phenomenon is answered in Figure 24. Curves showing the relationship between miles of line and source reactance for zero impedance viewed from the receiving end are shown. The curve for zero impedance to the third harmonic is shown to fall partially within the region of abnormality, near its upper extremity. Thus in the abnormal region the system offers a low impedance to third-harmonic currents, and it makes little difference whether the receiving-end transformer has a closed delta or not, since in either case third-harmonic current can easily be supplied.

The preceding discussion would not be complete without some mention of why the second-harmonic shunt is so effective in reducing the abnormal condition in the long lines case. In the single-phase case, the third-harmonic region of overvoltages could be eliminated by a third-harmonic resonant shunt, while the second-harmonic region could be eliminated by a second-harmonic shunt. By analogy between the single-phase and three-phase circuits it is evident that the abnormal region shown in Figure 24 or Figure 6 can be eliminated by a second-harmonic shunt. Furthermore, for reasons just mentioned, with the delta closed, no region of abnormality appears for the third harmonic. Consequently, for the delta closed condition, which is the practical case, a second-harmonic shunt is completely effective in eliminating the abnormal condition brought about by exciting power transformers through long lines.

The preceding analysis has dealt with only the second and third harmonics. Higher harmonics, when suppressed by systems offering infinite impedance to them will produce voltage distortions, but the overvoltages produced will in general be of no practical significance as compared to the phenomena as discussed in this paper.

While the foregoing discussion has dealt primarily with long lines carrying magnetizing currents, it is possible that

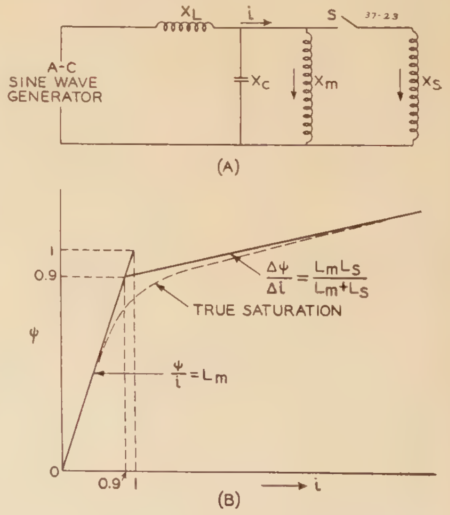


Figure 23. Equivalent circuit (system of Figure 21) for visualizing the underlying cause of the phenomenon

the phenomena observed may be encountered in other cases when the load requires harmonic currents to flow through long lines or from sources which tend to suppress them.

When series capacitors are present, the impedance of the system viewed from the receiving-end transformer terminals remains only slightly changed as far as harmonics are concerned. This is readily understood, because with any practical series-capacitor compensation the reactance of the series capacitor varies inversely as the frequency. Consequently, in the region which caused voltage instability without series capacitors, it is to be expected that instability of the same type will be found with series capacitors. In addition, the presence of series capacitors introduces the possibility of series resonant conditions in a nonlinear circuit consisting essentially of source and line reactance, series capacitor, and saturable reactor (transformer) which accounts for the broadening of the region of abnormality shown in Figure 17. This circuit has been discussed in the technical press, and its behavior is generally well understood.[4-8] It generally requires some form of "shock" excitation in order for the subharmonics to appear. That is, there are several modes of ultimate behavior depending on the initial conditions. This is in contrast to the phenomenon characteristic of the circuit shown in Figure 21 which is discussed in this paper and which generally does not require shock excitation for the phenomenon to appear. The harmonics and subharmonics appear spontaneously, within a few cycles generally, and represent the only mode of steady-state behavior for the circuit. That is, the circuit can respond only in accordance with the curves of Figures 4a and 4b whether the system voltage is raised gradually or whether the system is suddenly energized.

Appendix I

In the equivalent circuit of Figure 19, the per-unit current i_S (base current $=1/L_m$) through the switch following the closing of it at an initial angle θ after a positive voltage crest (assumed to the unity) is

$$i_{S1}=A_1\sin(t+\theta_1)+B_1\sin(\omega_C t+\phi_1)+D_1 \qquad (1)$$

where

$$A_1=\frac{X_m(\omega_o^2-1)}{X_s(\omega_C^2-1)}$$

$$B_1=\frac{X_m(\omega_o^2-\omega_C^2)}{X_s\omega_C^2(1-\omega_C^2)}\sqrt{\sin^2\theta_1+\omega_C^2\cos^2\theta_1}$$

$$D_1=-\frac{X_m\omega_o^2\sin\theta_1}{X_s\omega_C^2}$$

$$\omega_o=\sqrt{\frac{X_c(X_m+X_L)}{X_mX_L}}$$

the circuit natural frequency in per unit with switch S open

$$\omega_C=\sqrt{\frac{X_c(X_sX_m+X_sX_L+X_mX_L)}{X_mX_LX_s}}$$

the circuit natural frequency in per unit with the switch S closed

$$\sin\phi_1=\frac{\sin\theta_1}{\sqrt{\sin^2\theta_1+\omega_C^2\cos^2\theta_1}}$$

$$\cos\phi_1=\frac{\omega_C\cos\theta_1}{\sqrt{\sin^2\theta_1+\omega_C^2\cos^2\theta_1}}$$

The current i flowing into the equivalent transformer is

$$i_{c1}=A_1'\sin(t+\theta_1)+B_1'\sin(\omega_C t+\phi_1)+D_1' \qquad (2)$$

where

$$A_1'=\left(1+\frac{X_s}{X_m}\right)A_1$$

$$B_1'=\left(1+\frac{X_s}{X_m}\right)B_1$$

$$D_1'=\sin\theta_1+\left(1+\frac{X_s}{X_m}\right)D_1$$

The voltage across the capacitor is

$$e_{c1}=F_1\cos(t+\theta_1)+G_1\cos(\omega_C t+\phi_1) \qquad (3)$$

where

$$F_1=\frac{(\omega_o^2-1)}{(\omega_C^2-1)}$$

$$G_1=\frac{(\omega_o^2-\omega_C^2)}{\omega_C(1-\omega_C^2)}\sqrt{\sin^2\theta_1+\omega_C^2\cos^2\theta_1}$$

Interrupting the current i_S, the voltage across the capacitor is

$$\begin{aligned}e_{o1}&=\cos(t+\theta_1')+H_1\sin\omega_o t+J_1\times\\&\qquad\cos(\omega_o t+\alpha_1)+K_1\cos(\omega_o t+\beta_1) \qquad (4)\\&=\cos(t+\theta_1')+M_1\cos(\omega_o t+\Sigma)\end{aligned}$$

where

$$H_1=\frac{(\omega_o^2-\omega_C^2)\omega_o\sin\theta_1}{\omega_C^2}$$

$$J_1=\frac{(\omega_o^2-\omega_C^2)}{\omega_C^2(1-\omega_C^2)}\sqrt{\sin^2\theta_1+\omega_C^2\cos^2\theta_1}\times\sqrt{\omega_C^2\cos^2\delta_1+\omega_o^2\sin^2\delta}$$

$$K_1=\frac{(\omega_o^2-\omega_C^2)}{(\omega_C^2-1)}\sqrt{\cos^2\theta_1'+\omega_o^2\sin^2\theta_1'}$$

$$\delta_1=\omega_C t_{c1}+\phi_1$$
$$\theta_1'=t_{c1}+\theta_1$$

t_{c1} is the interval of time in radians during which the switch was closed

$$\sin\alpha_1=\frac{\omega_o\sin\delta_1}{\sqrt{\omega_C^2\cos^2\delta_1+\omega_o^2\sin^2\delta_1}}$$

$$\cos\alpha_1=\frac{\omega_C\cos\delta_1}{\sqrt{\omega_C^2\cos^2\delta_1+\omega_o^2\sin^2\delta_1}}$$

$$\sin\beta_1=\frac{\omega_o\sin\theta_1'}{\sqrt{\cos^2\theta_1'+\omega_o^2\sin^2\theta_1'}}$$

$$\cos\beta_1=\frac{\cos\theta_1'}{\sqrt{\cos^2\theta_1'+\omega_o^2\sin^2\theta_1'}}$$

The current i becomes

$$\begin{aligned}i_{o1}&=\sin(t+\theta_1)-\frac{H_1}{\omega_o}\cos\omega_o t+\frac{J_1}{\omega_o}\times\\&\qquad\sin(\omega_o t+\alpha_1)+\frac{K_1}{\omega_o}\sin(\omega_o t+\beta_1) \qquad (5)\end{aligned}$$

Closing the switch at the next instant when the current i reaches the critical value

$$\begin{aligned}i_{S2}&=A_1\sin(t+\theta_2)+B_2\sin(\omega_C t+\phi_2)+\\&\qquad D_2+M_1S_2\sin(\omega_C t+\psi_2)+W_2M_1 \qquad (6)\end{aligned}$$

A_1 defined in equation 1

$$B_2=\frac{X_m(\omega_o^2-\omega_C^2)}{X_s\omega_C^2(1-\omega_C^2)}\sqrt{\sin^2\theta_2+\omega_C^2\cos^2\theta_2}$$

$$D_2=-\frac{X_m\omega_o^2\sin\theta_2}{X_s\omega_C^2}$$

$$\theta_2=t_{o1}+t_{c1}+\theta$$

$$\sin\phi_2=\frac{\sin\theta_2}{\sqrt{\sin^2\theta_2+\omega_C^2\cos^2\theta_2}}$$

$$\cos\phi_2=\frac{\omega_C\cos\theta_2}{\sqrt{\sin^2\theta_2+\omega_C^2\cos^2\theta_2}}$$

$$S_2=\frac{X_m}{\omega_C^2X_s}\sqrt{\omega_o^2\sin^2\Delta_2+\omega_C^2\cos^2\Delta_2}$$

$$\sin\psi_2=\frac{\omega_o\sin\Delta_2}{\sqrt{\omega_o^2\sin^2\Delta_2+\omega_C^2\cos^2\Delta_2}}$$

$$\cos\psi_2=\frac{\omega_C\cos\Delta_2}{\sqrt{\omega_o^2\sin^2\Delta_2+\omega_C^2\cos^2\Delta_2}}$$

$\Delta_2=$ angle of ω_o frequency voltage past positive voltage crest when switch is closed

$$W_2=-\frac{X_m\omega_o\sin\Delta_2}{X_s\omega_C^2}$$

The voltage across the capacitor is

$$e_{c2}=\frac{X_s}{X_m}\frac{di_{S2}}{dt}$$

and the current i_{c2} into the equivalent transformer is of the form of equation 2.

At the time of interruption, the current i_{o2} takes the form of equation 5 subject to the new boundary conditions. The voltage e_{o2} takes the form of equation 4 also subject to the new boundary conditions.

This procedure can go on for as long as desired, each time imposing the new boundary conditions. It can be seen readily that these successive boundary conditions will not in general be the same for adjacent cycles of fundamental frequency. That is $\theta_1+\pi\neq\theta_2$, $\theta_2+\pi\neq\theta_3$, ... $\theta_n+\pi\neq\theta_{n+1}$. Ultimately, however, $\theta_n+a\pi=\theta_{n+a}$ and a definite subharmonic frequency will appear. Furthermore, the voltage oscillation across the capacitor will be of fundamental frequency plus a natural-frequency oscillation which is predominantly that defined as ω_o, the natural frequency of the system with the switch open. This is brought about by the fact that the switch S is closed in general only a short fraction of a fundamental-frequency period, and that period is generally in the vicinity of fundamental-frequency voltage zero. Therefore, it is to be expected that when circuit losses are low, the natural frequency of the circuit with the switch open and subharmonics will appear across the capacitor in the circuit of Figure 23.

Appendix II

Viewed from the receiving end, the system of Figure 1 or 2 neglecting losses offers an impedance[9]

$$Z_r=j\frac{\omega L_s\cos\omega l\sqrt{LC}+\sqrt{\frac{L}{C}}\sin\omega l\sqrt{LC}}{\cos\omega l\sqrt{LC}-\omega L_s\sqrt{\frac{C}{L}}\sin\omega l\sqrt{LC}} \qquad (1)$$

where

L = inductance per mile in henrys
C = capacitance per mile in farads
L_s = source inductance in henrys
$\omega=2\pi f$
l = length of line in miles

In the transient-analyzer artificial line

$L=0.002$ henry per mile
$C=0.016$ microfarad per mile

For $Z_r=\infty$, the criterion is

$$\cos\omega l\sqrt{LC}-\omega L_s\sqrt{\frac{C}{L}}\sin\omega l\sqrt{LC}=0 \qquad (2)$$

or, rewriting

$Zr=\infty$

when

$$\tan\omega l\sqrt{LC}=\frac{1}{\omega L_s}\sqrt{\frac{L}{C}} \qquad (3)$$

This equation was used with the above numerical constants for plotting the curves relating source reactance and miles of line to give infinite impedance in Figure 20.

For $Z_r=0$, the criterion is

$$\omega L_s\cos\omega l\sqrt{LC}+\sqrt{\frac{L}{C}}\sin\omega l\sqrt{LC}=0 \qquad (4)$$

or, rewriting

$Z_r=0$

when

$$\omega l\sqrt{LC}=-\omega L_s\sqrt{\frac{C}{L}} \qquad (5)$$

This equation was used with the above numerical constants for plotting the curves relating source reactance and miles of line to give zero impedance in Figure 24.

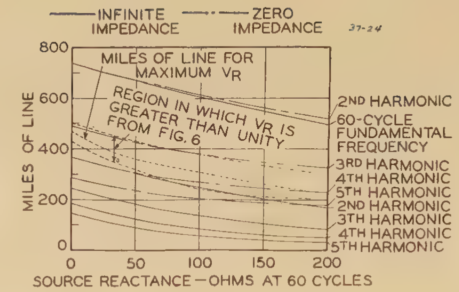


Figure 24. Relationship between miles of line and source reactance which offers infinite or zero impedance to harmonics viewed from any point along the line

Resistance neglected

It can also be shown that the relationship between source reactance ωL_s and line length to give infinite impedance is independent of the point considered along the line. If l is the total length of line considered, and d is the distance between the sending end and the point from which the impedance is to be determined, then

$$Z=\frac{-j\sqrt{\frac{L}{C}}\cot\omega(l-a)\sqrt{LC}\cdot j\times\left[\frac{\omega L_s\cos\omega d\sqrt{LC}+\sqrt{\frac{L}{C}}\sin\omega d\sqrt{LC}}{\cos\omega d\sqrt{LC}-\omega L_s\sqrt{\frac{C}{L}}\sin\omega d\sqrt{LD}}\right]}{-j\sqrt{\frac{L}{C}}\cot\omega(l-d)\sqrt{LC}+j\times\left[\frac{\omega L_s\cos\omega d\sqrt{LC}+\sqrt{\frac{L}{C}}\sin\omega d\sqrt{LC}}{\cos\omega d\sqrt{LC}-\omega L_s\sqrt{\frac{C}{L}}\sin\omega d\sqrt{LC}}\right]}$$

$$-\sqrt{\frac{L}{C}}\cot\omega(l-d)\sqrt{LC}\times$$

$$=\frac{\left[\omega L_s\cos\omega d\sqrt{LC}+\sqrt{\frac{C}{L}}\sin\omega d\sqrt{LC}\right]}{\left[-\sqrt{\frac{L}{C}}\cot\omega(l-d)\sqrt{LC}\right]\times}+$$

$$\left[\cos\omega d\sqrt{LC}-\omega L_s\sqrt{\frac{C}{L}}\sin\omega d\sqrt{LC}\right]\times$$

$$\omega L_s\cos\omega d\sqrt{LC}+\sqrt{\frac{L}{C}}\sin\omega d\sqrt{LC} \qquad (6)$$

$Z=\infty$ when the denominator of equation 6 is zero. Rewriting this denominator

$$-\sqrt{\frac{L}{C}}\cot\omega(l-d)\sqrt{LC}\cdot\cos\omega d\sqrt{LC}+$$
$$\omega L_s\cot\omega(l-d)\sqrt{LC}\cdot\sin\omega d\sqrt{LC}+$$
$$\omega L_s\cos\omega d\sqrt{LC}+\sqrt{\frac{L}{C}}\sin\omega d\sqrt{LC}$$

$$=-\sqrt{\frac{L}{C}}\cos\omega d\sqrt{LC}\times$$
$$\frac{1+\tan\omega l\sqrt{LC}\tan\omega d\sqrt{LC}}{\tan\omega l\sqrt{LC}-\tan\omega d\sqrt{LC}}+\omega L_s\times$$
$$\sin\omega d\sqrt{LC}\,\frac{1+\tan\omega l\sqrt{LC}\tan\omega d\sqrt{LC}}{\tan\omega l\sqrt{LC}-\tan\omega d\sqrt{LC}}+$$
$$\omega L_s\cos\omega d\sqrt{LC}+\sqrt{\frac{L}{C}}\sin\omega d\sqrt{LC}=0$$

$$-\sqrt{\frac{L}{C}}\cos\omega d\sqrt{LC}-\sqrt{\frac{L}{C}}\cos\omega d\sqrt{CL}\times$$
$$\tan\omega l\sqrt{LC}\tan\omega d\sqrt{LC}+\omega L_s\sin\omega d\sqrt{LC}+$$
$$\omega L_s\sin\omega d\sqrt{LC}\tan\omega l\sqrt{LC}\tan\omega d\sqrt{LC}+$$
$$\omega L_s\cos\omega d\sqrt{LC}\tan\omega l\sqrt{LC}-$$
$$\omega L_s\cos\omega d\sqrt{LC}\tan\omega d\sqrt{LC}+$$
$$\sqrt{\frac{L}{C}}\sin\omega d\sqrt{LC}\tan\omega l\sqrt{LC}-$$
$$\sqrt{\frac{L}{C}}\sin\omega d\sqrt{LC}\tan\omega d\sqrt{LC}=0$$

$$\omega L_s[\sin\omega d\sqrt{LC}+\sin\omega d\sqrt{LC}\tan\omega l\sqrt{LC}\times$$
$$\tan\omega d\sqrt{LC}+\cos\omega d\sqrt{LC}\tan\omega l\sqrt{LC}-$$
$$\cos\omega d\sqrt{LC}\tan\omega d\sqrt{LC}]$$
$$=\sqrt{\frac{L}{C}}[\cos\omega d\sqrt{LC}+\cos\omega d\sqrt{LC}\tan\omega l\times$$
$$\sqrt{LC}\tan\omega d\sqrt{LC}-\sin\omega d\sqrt{LC}\ \tan\omega l\times$$
$$\sqrt{LC}+\sin\omega d\sqrt{LC}\tan\omega d\sqrt{LC}]\omega L_s\times$$
$$\tan\omega l\sqrt{LC}[\sin\omega d\sqrt{LC}\tan\omega d\sqrt{LC}+$$
$$\cos\omega d\sqrt{LC}]=\sqrt{\frac{L}{C}}[\cos\omega d\sqrt{LC}+$$
$$\sin\omega d\sqrt{LC}\tan\omega d\sqrt{LC}]$$

and finally $\tan\omega l\sqrt{LC}=\dfrac{1}{\omega L_s}\sqrt{\dfrac{L}{C}}$

which is identical with equation 3, which was to be proved.

Similarly it can be shown that the condition for $Z=0$ as given by equation 4 holds for any location along the line.

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THE transmission of electric power over long distances has been the subject of considerable study,[1] and it has been concluded that an effective way of increasing the permissible straightaway transmission distance is by means of series-capacitor compensation of part of the transmission-line inductive reactance. The introduction of longer lines together with capacitor compensation brings up several technical problems which must be considered if proper operation is to be assured. Studies of transient stability and switching times following faults and of abnormal overvoltages which may result from interaction of line capacitance and transformer exciting impedance at no load are presented in references 2 and 3 respectively. This paper presents the results of a study of hunting and self-excitation during normal operation as affected by line and machine characteristics.

Previous work[4] and field experience have indicated the possibility of self-excited oscillations when synchronous machines are connected to capacitor-compensated circuits. It has been shown[5] that a high ratio of line resistance to reactance reduces the machine damping torque, and that the compensation of the line reactance by means of series capacitors increases the effective value of this ratio. It has also been shown[4,6,7] that series-capacitor compensation of line reactance may lead to self-excited electrical oscillations as well as to mechanical rotor oscillations (hunting) under certain circuit conditions.

The series-capacitor-compensated long-distance transmission system is somewhat difficult to analyze directly in terms of previously developed criteria, as in this case there is a considerable distributed shunt capacitance in addition to the series capacitance. This shunt capacitance has been neglected in previous mathematical analyses because of the analytical difficulties involved as well as for the reason that in the previously considered application to distribution circuits[4] the shunt capacitance was relatively small and unimportant. Setting up and solving directly the complete differential equations of the system including a synchronous generator and compensated transmission line are impractical, if the transmission line is represented with any reasonable degree of exactness, as the order is extremely high. The alternative of setting up and studying a laboratory model, as reported in a recent paper[8] and as was done in a previous unpublished study, was not considered desirable because of the difficulty of varying system parameters. Instead it seemed preferable to develop and apply the method of equivalent circuits, using the a-c network analyzer, which had already shown itself to be of practical value in other related studies.[9,10] The equivalent-circuit method has the advantage of ease of varying system parameters over a wide range, as in mathematical analyses, but with a minimum of calculation, as in model testing, since most of the results can be read directly on instruments. The network analyzer is particularly convenient, compared to hand calculations, in the necessary setting up of initial conditions, that is, machine excitations, fluxes, currents, and so forth, which vary with practically every change of any system parameters for a specified system load angle, or other condition.

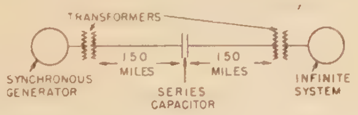

Figure 1. Series-capacitor-compensated transmission system

Transformer reactance = ten per cent (each)
Line reactance = 0.8 ohm per mile
Line resistance = 0.08 ohm per mile
Line capacitance = 5.2×10^{-6} mho per mile
Generator $x_d = 110$ per cent, $x_q = 74$ per cent
$x_d' = 37.7$ per cent
$x_d'' = 28.1$ per cent, $x_q'' = 39.3$ per cent (with amortisseur)
Base kilovolt-amperes = $2.5\times(\text{kv})^2$

Conclusions

1. A method of attack using equivalent circuits set up on the a-c network analyzer has been developed which enables one to predetermine from an equivalent circuit the possibility of self-excited oscillations in generators connected to series-capacitor-compensated long-distance transmission lines. A detailed study of any proposed installation can be made by this method, if it appears necessary from a preliminary inspection.

2. In the range of transmission line studied (up to 300 miles and fully compensated) hunting can be prevented by a proper generator amortisseur without encountering electrical self-excitation. An amortisseur is necessary if hunting is to be avoided under all conditions, but self-excitation will not occur unless the line resistance is lower than for usual practical transmission lines.

3. Rotor inertia has little effect on the significant regions of electrical self-excitation.

Procedure

Equivalent circuits for the synchronous generator and for the compensated transmission line during oscillations are developed and described in the companion paper[11] and in reference 12, respectively. Only the procedure used in applying the method to the network analyzer will be discussed here.

The system for which results are presented is shown in Figure 1. It consists of a 300-mile transmission line terminated at both ends by a transformer of ten per cent reactance. A generator having the electrical characteristics of an average water-wheel generator is connected through the transmission line and transformers to an infinite bus. The damping and synchronizing torque coefficients T_d and T_s for a range of oscillation frequencies h have been obtained for several cases. The limits of stable operation are determined in terms of the stable ranges of generator inertia as follows.

For the generator, the torque equation during sinusoidal rotor oscillations may be expressed as:

$$(-Mh^2+jhT_d+T_s)\Delta\delta=\Delta T \qquad (1)$$

where

M = generator rotor inertia, per unit = $4\pi fH$
h = oscillation frequency, per unit
$\Delta\delta$ = rotor angular oscillation
ΔT = applied torque required to produce the oscillation $\Delta\delta$

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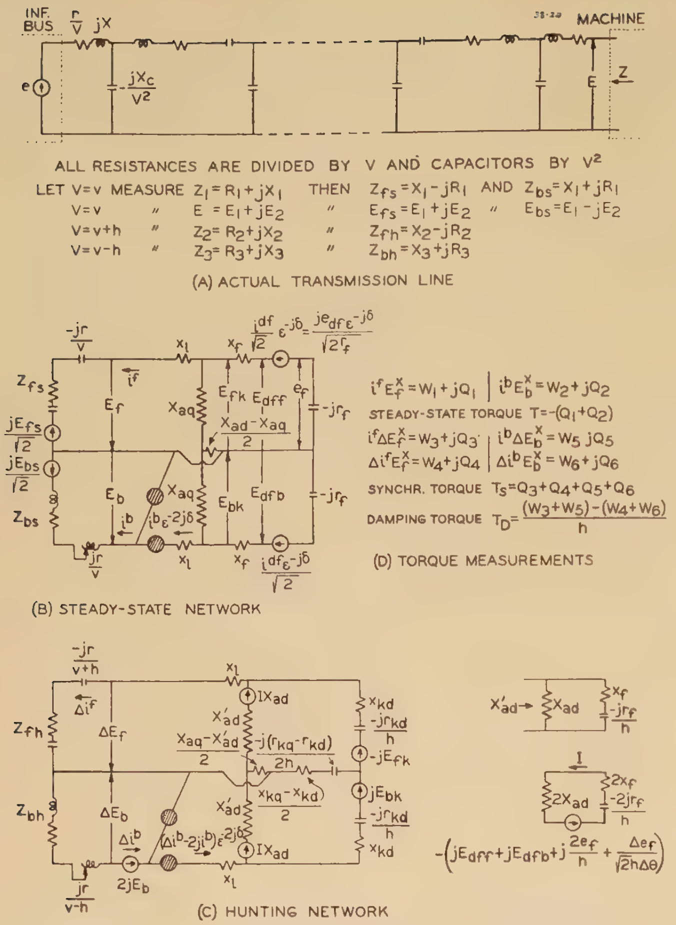

INF. BUS
MACHINE
ALL RESISTANCES ARE DIVIDED BY V AND CAPACITORS BY V²
LET V=v MEASURE Z₁=R₁+jX₁ THEN Z_fs=X₁−jR₁ AND Z_bs=X₁+jR₁
V=v E=E₁+jE₂ E_fs=E₁+jE₂ E_bs=E₁−jE₂
V=v+h Z₂=R₂+jX₂ Z_fh=X₂−jR₂
V=v−h Z₃=R₃+jX₃ Z_bh=X₃+jR₃
(A) ACTUAL TRANSMISSION LINE
(B) STEADY-STATE NETWORK
STEADY-STATE TORQUE T=−(Q₁+Q₂)
SYNCHR. TORQUE T_S=Q₃+Q₄+Q₅+Q₆
DAMPING TORQUE T_D=((W₃+W₅)−(W₄+W₆))/h
(D) TORQUE MEASUREMENTS
(C) HUNTING NETWORK

Figure 2. Equivalent circuits for transmission line connected to salient-pole synchronous machine

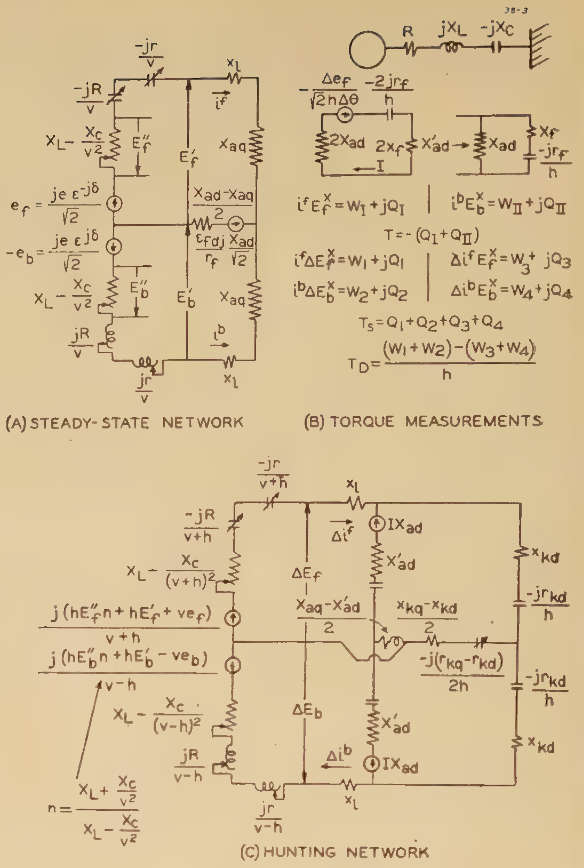


Figure 3. Equivalent circuits for salient-pole synchronous machine connected to infinite bus through *R-L-C* tie

At the boundary between stable and unstable oscillations the total rotor torque must be zero ($\Delta T=0$). Equation 1 then shows that T_d must be zero, and T_s must equal h^2M (or M must equal T_s/h^2). Thus at each point where T_d crosses zero, a limiting value of M may be found. For low values of h and positive T_s it has previously been shown that the system is unstable for negative values of T_d. This provides the information required to distinguish between the stable and unstable ranges of rotor inertia. The magnitude of the negative damping torque coefficient T_d also provides a measure of the degree of instability, just as the magnitude of the positive damping torque coefficient is a measure of the amount of negative damping which can be tolerated in the system. In the region of high oscillation frequency no such simple and foolproof criterion of stability can be found.

From the previous studies (references 4, 6, 7, 13), it is known that electrical self-excitation may occur even aside from rotor oscillations, that is, even with infinite rotor inertia. In this case the electrical-network effective impedance becomes zero[14] at the self-excited oscillation frequency, and the torque developed becomes infinite. If, however, the system will not self-excite except with a finite rotor inertia M, the limiting value of M may be found as described above. With any reasonable value of M the torques in the region of self-excitation are extremely great, and the transition from stable to unstable operation can thus easily be detected.

The equivalent circuits required are shown in detail in references 11, 12, and 15. Figure 2 shows the circuits as applied to the present case. Two points are to be noted in connection with the procedure used. First, the equivalent circuit for the transmission line is set up separately and its characteristic impedances and terminal voltages determined as indicated in Figure 2a over the desired range. These values are then used in the equivalent circuit for the synchronous machine, Figures 2b and 2c. Second, since the machine equivalent circuit as developed in references 12 and 15 can be so adjusted as to contain both positive and negative resistances but no capacitance, all impedance values are multiplied by a factor $-j$, resulting in the circuit shown in Figures 2b and 2c, which contains inductance, capacitance, and positive resistance over a good part of the range. The torque formulas undergo a similar transformation, as may be seen by comparing Figure 2d with the formulas of reference 12. For large values of the oscillation frequency h (that is, for $h>(1-\sqrt{x_c/x_l})$ approximately, where x_l is the total line, transformer and machine armature leakage reactance), the impedance Z_{bh} of Figure 2c has a negative-resistance component X_3. These cases were handled by a method of superposition, by first measuring the circuit impedance viewed from the Δi^b mesh and then making the necessary additional calculations by hand.

Results

Figures 4–7 show some of the results obtained.

Figure 4 gives damping and synchronizing torques for the system with line capacitance neglected. Here there is no possibility of purely electrical self-excitation, but it is seen that for oscillation frequencies in the range $0.5<h<1.0$ the damping torque coefficient is negative, so that hunting will occur for rotor inertias which give natural frequencies in this range. From the formula $M=T_s/h^2$, applied at the points of zero damping, the

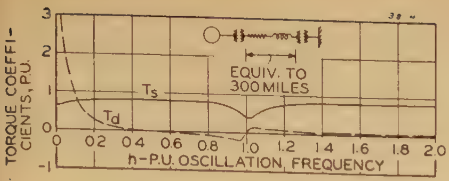


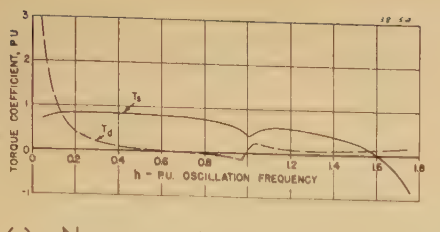
Figure 4. Damping and synchronizing torque coefficients for synchronous machine connected to infinite bus through series resistance and reactance—at no load and with amortisseur

range of rotor inertias for which the system is unstable is $0.43 < M < 3.24$, or in terms of the conventional inertia constant $H(M = 4\pi fH)$, $0.00057 < H < 0.0043$. These values are extremely small and entirely outside of any practical range, so the conclusion is that the system is stable. This would be expected for the machine considered, since it has an amortisseur.

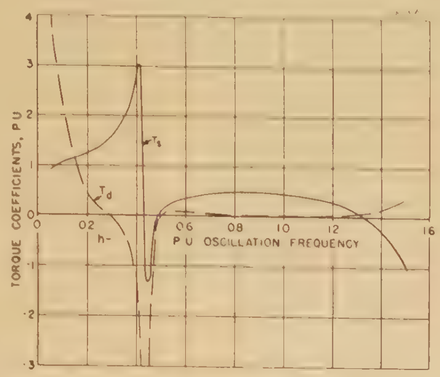
Figure 5 shows the effect of varying the amount of series-capacitor compensation for the same system as in Figure 4 but now considering the transmission-line capacitance. The line capacitance does not by itself affect the range of negative damping very much, as may be seen by comparing Figure 5a with Figure 4a. Figures 5b to 5d show that one effect of series-capacitor compensation is to extend the lower limit of rotor inertia for which the system is unstable to zero, as seen by the fact that T_s becomes zero while T_d is still negative. However, the upper limit is still extremely small; in the most severe case of full compensation of line and transformer reactance ($x_c = 0.8$, Figures 5d and 5h) the greatest rotor inertia for which the system will be unstable is $M = T_s/h^2 = 5.05/(0.19)^2 = 140$, or $H = 0.186$.

The fact that T_s becomes negative and that resonance points in both T_d and T_s occur at high oscillation frequencies does not mean that the system is unstable. The curves of hT_d versus T_s given in Figures 5e to 5h are useful in this respect as they exhibit a remarkable regularity, forming more or less circular curves in the resonance regions and thus serving as a good indication of the magnitude of the torque coefficients and the probable form of the T_d and T_s versus h curves in these regions.

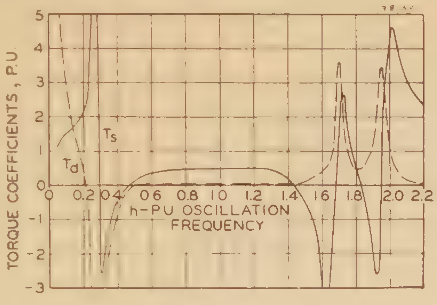
Figure 6 is similar to Figure 5 but for a generator with no amortisseur. The damping now becomes negative at a much lower oscillation frequency, corresponding to a rotor inertia approaching practical values; Figure 7 shows the effect of load angle on the system of Figure 6. Even with 75 per cent load on the generator the system is still unstable at rather low oscillation frequencies.



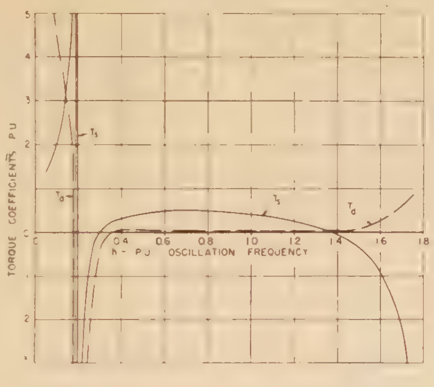
(a). No compensation, $x_c = 0$ (see Figure 5e)



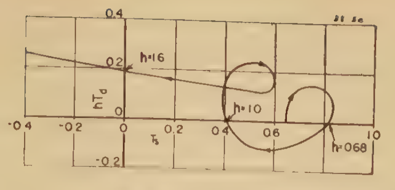
(b). With series-capacitor compensation, $x_c = 40$ per cent (see Figure 5f)



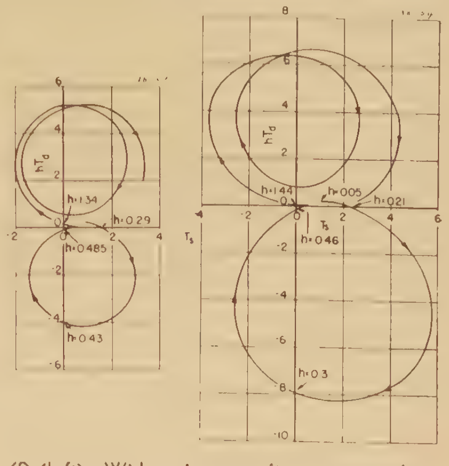
(c). With series-capacitor compensation, $x_c = 60$ per cent (see Figure 5g)



(d). With series-capacitor compensation, $x_c = 80$ per cent (see Figure 5h)

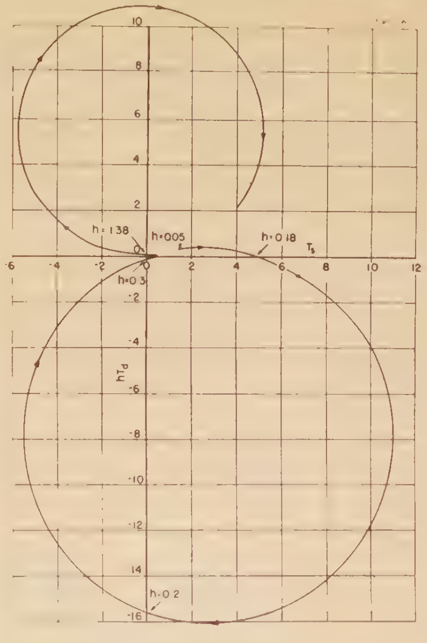


(e). No compensation, $x_c = 0$ (see Figure 5a)



(f) (left). With series-capacitor compensation, $x_c = 40$ per cent (see Figure 5b)

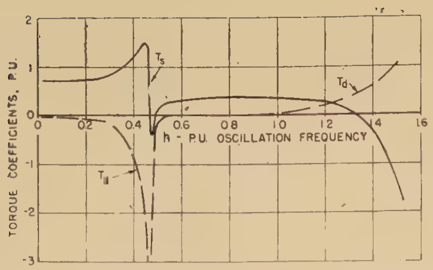
(g) (right). With series-capacitor compensation, $x_c = 60$ per cent (see Figure 5c)



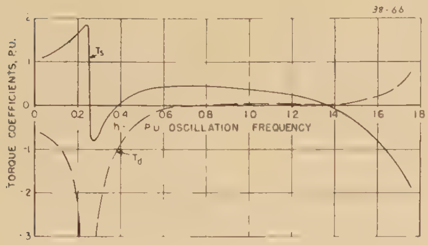
(h). With series-capacitor compensation, $x_c = 80$ per cent (see Figure 5d)

Figure 5. Damping and synchronizing torque coefficients for synchronous machine connected to infinite bus through 300-mile transmission line—at no load and with amortisseur

In all of the cases considered above the armature and line resistance are so high that electrical self-excitation does not occur, in spite of the fact that a rather low ($r/x = 10$ per cent) line resistance was assumed. Previously[12] it had been concluded that the line resistance below which self-excitation could exist with infinite rotor inertia and zero shunt line capacitance was well below the actual probable value. The results presented above lead to this same conclusion for an actual line and considering rotor inertia. As a quantitative check of the effect of rotor inertia the case of Figure 2, reference 12, has been set up and studied by the equivalent-circuit method, using the circuit of Figure 3. The results obtained show that the introduction of a



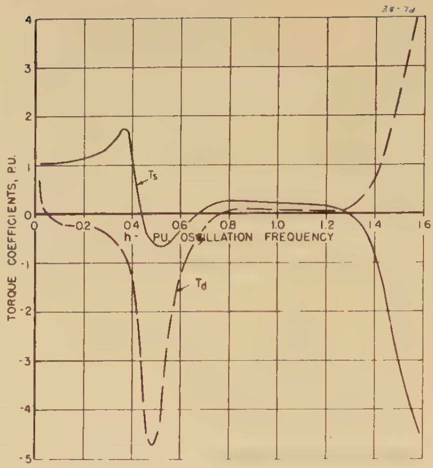
(a). With series-capacitor compensation; $x_c=40$ per cent



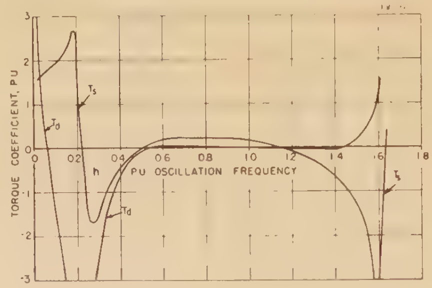
(b). With series-capacitor compensation, $x_c=80$ per cent

Figure 6. Damping and synchronizing torque coefficients for synchronous machine connected to infinite bus through 300-mile transmission line—at no load and without amortisseur

finite rotor inertia makes very little difference in the critical line resistance even for inertias considerably smaller than could actually exist. In terms of equation 1 and the curves of hT_d versus T_s, it is evident that T_s must be practically infinite at the boundary of self-excitation except for very small oscillation frequency h, since reasonable per unit values of inertia are always large. The series of curves of Figures 5e to 5h shows the growth of the lower circle as the compensation x_c is increased and the region of electrical self-excitation is approached. At the critical value when self-excitation is just possible this circle



(a). With series-capacitor compensation, $x_c=40$ per cent



(b). With series-capacitor compensation, $x_c=80$ per cent

Figure 7. Damping and synchronizing torque coefficients for synchronous machine connected to infinite bus through 300-mile transmission line—at 75 per cent load and without amortisseur

becomes infinite, and in the unstable region it curves upward rather than downward and so does not intersect the $hT_d=0$ axis.

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Dielectric-Recovery Characteristics of Large Air Gaps

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Synopsis: In contrast to power arcs in air, for which the dielectric-recovery voltage decreases with increasing arc current, arcs in air produced by surge currents of several hundred microseconds' duration possess dielectric-recovery characteristics that vary only slightly with current magnitude and wave shape. Even when the discharge current is prolonged at low magnitude for as long as a thousand microseconds, the rate of dielectric recovery is little affected. For this condition at times when current as high as one ampere is flowing, the transient voltage required to convert to a high current arc is quite high. The mechanism for such a discharge is different than for the breakdown of virgin air, and the initial arc drop is sufficiently high to record its transient character. It is about ten times the steady-state value at the time the crest current is reached and drops about 50 per cent in the first 20 microseconds.

Photographic studies show that even at short-time intervals successive discharges do not follow exactly the path blazed by the initial discharge but occur in an air column of low dielectric strength expanding radially with time. It is indicated that the high arc temperatures diffusing in this region can be the principal factor determining the rate of recovery after the first 1,000 microseconds.

A comparison is made with the mechanism of natural lightning particularly from recent data obtained on the low-magnitude portions of stroke currents between successive components. Such records show that current does not always flow continuously between multiple-stroke components for strokes to objects of normal height. The maximum interval of recorded zero current is about 23,000 microseconds. The rates of recovery for spark gaps indicate that about 40,000 microseconds is the upper limit for which the dielectric strength of the stroke channel will be sufficiently low to insure successive breakdown in the same general region.

The character of a lightning discharge initiating a power follow does not affect the rate of recovery after the power arc has been extinguished. Circuit reclosing times should be based primarily on the probability of the duration of multiple strokes and the probability of restriking for power arcs.

Correlation of the rate of dielectric recovery of air gaps with rates of system recovery show that an appreciable number of transmission-line lightning flashovers can occur at such a time relative to the normal system voltage cycle that only a very short-duration fault is produced. The number is about 10 to 15 per cent for a typical system having a natural period of 1,000 cycles which corresponds to a line about 40 miles in length. This is about half of the flashovers which normally do not cause outages. The rest are due to cases where power arcs longer than one cycle are produced but extinguish before a circuit-breaker operation.

THE rates at which large air gaps recover dielectric strength after discharging power-frequency currents and short-duration surge currents comparable to those produced by lightning are of fundamental importance in determining power-system performance. Some of the more important considerations are the factors governing the probability of lightning flashovers producing power follow, the probability of power follow producing an outage, and the minimum safe reclosing time after a circuit interruption. Because of the similarity between long arcs and the mechanism of natural lightning, data on the electrical characteristics of the arc path after an initial discharge and the character of subsequent discharges should also give information on the mechanism of multiple strokes. Despite its fundamental value, very meager data are available on the rates of dielectric recovery of large gaps, especially for initial discharges having the character of lightning currents. Most studies have been confined to power arcs in very small gaps where arc conditions are quite different.

For the study of surge current discharges in large air gaps a method of synchronizing two generators has been developed so that surge current discharges can be produced in a gap and controlled surge voltages applied after specified time intervals. Data have thus been obtained for 6- and 11-inch rod gaps on the voltage necessary to produce a second discharge as a function of the time interval after the first. Eleven inches was the maximum spacing which could be tested with the facilities available. With negative polarity throughout, a wide range of initial current magnitudes and wave shapes was studied, the subsequently applied voltage having the standard $1^1/_2 \times 40$ wave shape. The same terminology (critical voltage), as used for the minimum breakdown in virgin air, is applied to the minimum breakdown after an initial discharge. This should lead to no confusion as long as the time interval after the initial discharge is specified. By raising the voltage above the critical value, volt-time curves were also obtained in the same manner as those for breakdown in virgin air. Through the use of a high-speed rotating camera and still cameras, data were also obtained on the visual characteristics of the initial and subsequent discharges.

Test Circuit

A schematic diagram of the surge generator circuits is shown in Figure 1. The initial discharge through the test gap is produced by the current generator and the subsequently applied potential by the voltage generator. The two generator trip circuits are synchronized by a rotating switch. As shown in Figure 1, the tripping cycle is started by shorting out gap *A*. When the arm of the rotating switch next passes through gap *B* the tripping circuit for the current generator is discharged. When the arm passes through gap *C*, the tripping circuit for the voltage generator is discharged, providing gap *B* is still passing current, which is assured by making the time constant of the first circuit long enough for the desired range of time intervals. In this way the two generators can be discharged only in the proper sequence. The time interval is controlled by the spacing between gaps *B* and *C* and the speed of the rotating arm driven by a variable-speed motor.

During the discharging of the current generator, the voltage generator must be isolated from the test circuit. The gap *D* of Figure 1 must have a higher breakdown value than the test gap, so that the voltage generator will not be prematurely discharged. The larger gap requires a higher discharge voltage than is to be applied to the test gap, and thus, as shown in the figure, only a portion of the generator voltage is tapped from the potentiometer circuit. If the impedance of the current generator is high enough that it does not distort the voltage of the voltage generator discharging through it in parallel with the test gap, the gap *E*

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may flash over on the second discharge. However, as most high current generators are of low impedance, gap *E* must have a higher breakdown value than the test gap during the second discharge. If such a large gap spacing will not permit the initial discharge of the current generator, a fiber tube and fuse wire can be used to permit the current generator to discharge before isolating it for the voltage generator discharge.

Dielectric-Recovery Voltage for Initial Discharges of Short Duration

The wave shapes of the initial discharge currents studied can be conveniently classified into two groups. The first group had durations less than 100 microseconds and were short compared to the time interval between the initial discharge and the application of voltage. The second had durations of the same order of magnitude as the time intervals, and at the lower range of intervals current was still flowing when voltage was applied. The shortest time interval between the beginning of the current discharge and the application of voltage that could be consistently reproduced with the rotating gap was about 500 microseconds. The initial discharges of short duration will be considered first. In Figure 2 are given the wave shapes of the currents which were used for the two gap lengths studied. As shown by Figure 2a, the currents for the six-inch gap spacing involved crest magnitudes ranging from 1,390 amperes to 23,000 amperes and charges ranging from 0.002 to 0.238 coulomb. Wave shapes of two general types were used—one in which the high current lasted only a fraction of a microsecond, and the other in which the high current was maintained for five or ten microseconds. This was done to determine the effect of the two factors—magnitude and duration.

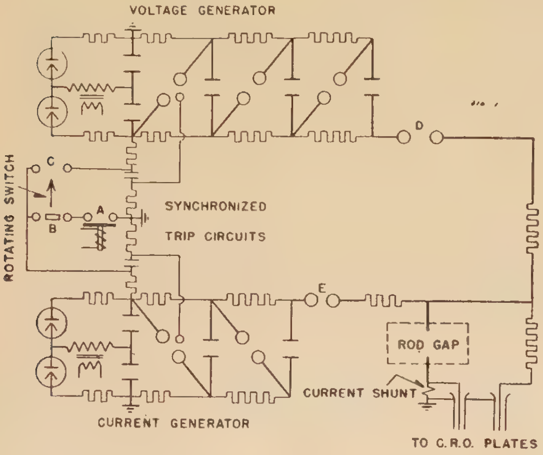


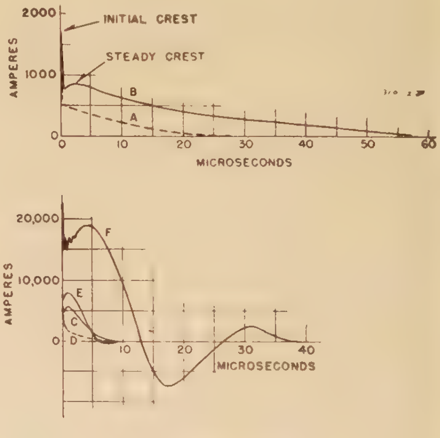
Figure 1. Schematic diagram of surge generators and synchronizing trip circuit

The discharges with only instantaneously high currents were produced with a high-voltage generator of low capacity and a high series resistance in the discharge circuit which limited the current flowing from the generator itself to a relatively low value. However, as previous work has shown, the discharge mechanism for large air gaps is quite similar to that of natural lightning.[1-3] The initial current flowing at the electrodes at the breakdown of an air gap is produced by streamers propagating from one or both electrodes and distributing charge along the channel which they blaze. When these two leaders meet, intense "return streamers" are formed which propagate back to the gap electrodes and rapidly discharge the charge in the gap. For applied voltages similar to the standard $1^1/_2$ x 40 wave, the whole process takes place in less than a microsecond. The high peak in the measured current occurs when the return streamers reach the gap electrodes, and the current which flows for the next fraction of a microsecond depends upon the charge in the air gap. After this initial charge is dissipated, the current is determined by the surge-generator circuit. Thus, for surge generators of low capacity and high discharge resistance, the current rapidly drops to a much lower magnitude designated in Figure 2 as the steady crest magnitude. The current waves *A*, *B*, *D*, and *H* were produced in this way. A high current generator of much greater capacity and lower discharge resistance was used to produce current waves *C*, *E*, *F*, and *I*, for which the sustained current magnitude is approximately the same as the initial crest.

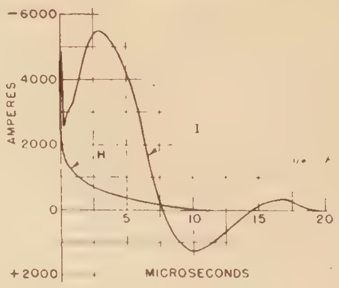
In Figures 3 and 4 are given data obtained on the critical voltage as a function of time interval after the initial discharge, and in Figure 5 are typical volt-time curves. For each time interval these data were obtained in the same manner as normally used for volt-time curves except that no correction was made for atmospheric conditions since the significance of this factor is not known. However, all data were taken under substantially uniform conditions, the relative air density varying between 0.994 and 1.01 and the absolute humidity between 0.152 and 0.426 inch of mercury vapor pressure.

CONSISTENCY OF BREAKDOWN

Caused principally by variations in ionization conditions, the breakdown of an air gap is not a single fixed value. There is a certain probability which determines the critical breakdown. If virgin air has less ionization than for a given period after an initial discharge, it would be thought that the variation of individual values from the critical would be appreciably less. Although no attempt was made to determine probability curves of breakdown, some information is contained in the data obtained for volt-time curves. For several cases the voltage was first set considerably below the break-



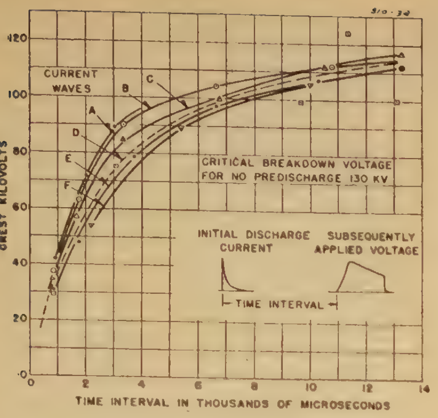
(a). 6-inch rod gap



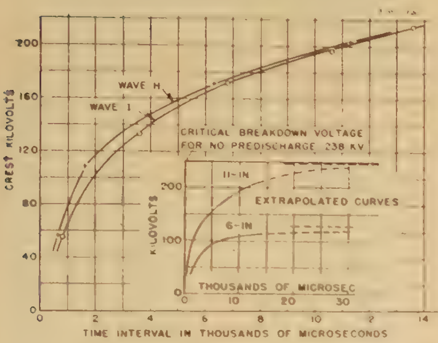
(b). 11-inch rod gap

Figure 2. Wave shapes of initial current discharges of short duration. Negative polarity

Wave	Initial Crest Amperes	Steady Crest Amperes	Charge in Coulombs
A	1,390	532	0.005
B	1,710	850	0.002
C	4,780	5,690	0.024
D	5,560	2,000	0.008
E	6,600	7,720	0.038
F	23,000	19,100	0.238
H	4,630	1,400	0.006
I	4,900	5,680	0.034



(a). 6-inch rod gap



(b). 11-inch rod gap

Figure 3. Critical breakdown voltage as function of time after initial discharges of Figure 2

Second applied voltage negative $1^1/_2 \times 40$ wave

down value, then raised in about two per cent steps, taking in some instances five and in others ten shots at each voltage. This provided data on the percentage variation, either in the minimum voltage that would produce breakdown, or the maximum that would not, from the mean or critical breakdown. These data showed, for time intervals below 1,000 microseconds, a maximum variation of about 20 per cent from the critical breakdown. Above 1,000 microseconds the maximum variation was about 13 per cent, and no trend was observed with time interval. However, in only four cases out of 30 was it over ten per cent and in only 13 over five per cent. Some of this variation might be due to irregularities in time interval as controlled by the rotating switch. However, for a given setting, the time interval did not vary more than about ± 25 microseconds which even at the lower range of time intervals would not, as shown by Figure 3, produce a variation in critical breakdown voltage of more than about two per cent. It is interesting that for intervals above 1,000 microseconds the spread of breakdown voltage for individual shots is no greater than observed when obtaining normal volt-time curves for virgin

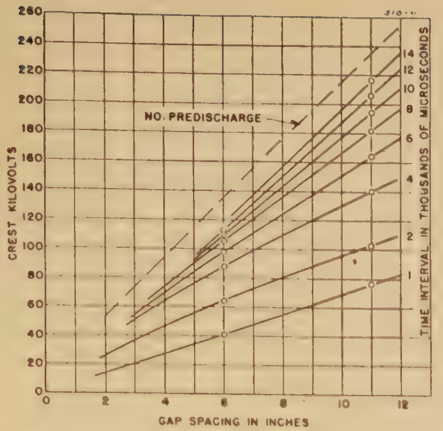


Figure 4. Critical breakdown for various time intervals as function of gap spacing

Wave *C* for 6-inch gap and wave *I* for 11-inch gap

air. This indicates that little ionization is left in the gap.

Probable Accuracy

The consistency with which the critical breakdown could be duplicated was about five per cent, and this is thought to be a conservative estimate of the probable accuracy of the curves of Figure 3. The accuracy of three points on Figure 3a for wave *D* at time intervals above 9,000 microseconds are considered questionable, because difficulty was experienced with synchronization of the trip circuits when they were obtained.

Effect of Initial Discharge Current

The curves of Figure 3 show that the critical breakdown voltage varies in a somewhat exponential manner with the time interval, increasing much more rapidly during the first 2,000 or 3,000 microseconds than thereafter. However, the maximum variation of the curves for the six-inch rod gap is remarkably small, despite the wide range of initial discharge currents. The maximum deviation from a mean occurs at the lower range of time intervals but is less than 20 per cent. After 5,000 microseconds it is less than ten per cent. Considering a mean curve for the six-inch gap, recovery to 50 per cent of the initial voltage strength occurs in about 2,500 microseconds and to 87 per cent in 13,000 microseconds. Since the deviation of the curves from one another is of the same order of magnitude as the accuracy of the data, it is difficult to determine the effect of current wave shape. As shown in Figures 2a and 3a, the higher the initial crest or the steady crest current, the lower the recovery voltage, except for the curves for waves *C* and *D*. Current magnitude appears to have more effect than charge, since some of the waves of higher initial current have much lower charge.

The manner in which the critical breakdown voltage for various time intervals increases with gap spacing is shown in Figure 4, where 6-inch and 11-inch rod-gap data for as nearly the same initial discharge current as could be reproduced are plotted. The data indicate that for any time interval the critical breakdown is approximately proportional to gap spacing. The volt-time curves of Figure 5 which are typical of those obtained show little relative flattening out with decreasing time interval. It might be expected that as the ionization remaining in the gap increases with decreasing time interval, the volt-time curves would become markedly flatter. Below about 2,000 microseconds they flatten out in regard to absolute differences in voltage but not in relative voltage change. Above

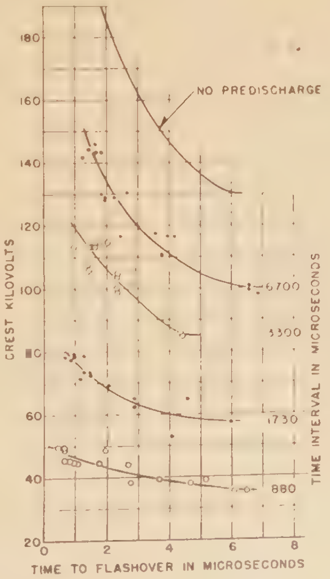
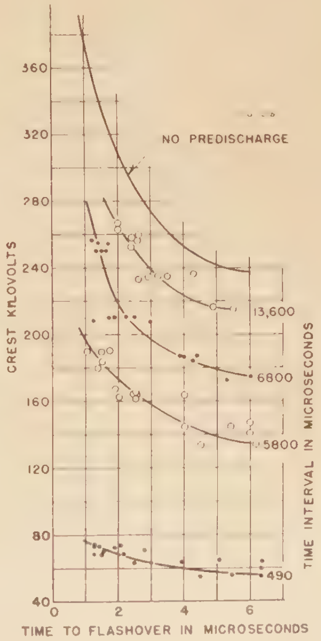


Figure 5. Volt-time curves for second voltage as applied at various time intervals

(a) (left). 6-inch rod gap

(b) (right). 11-inch rod gap



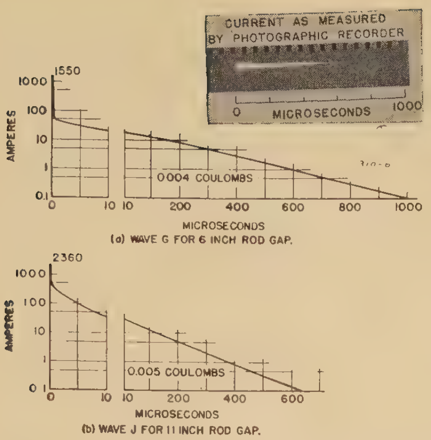


Figure 6. Wave shapes of initial discharges of long duration

3,000 microseconds the curves are approximately the same shape simply displaced in magnitude. This is further indication that little ionization is present at these time intervals.

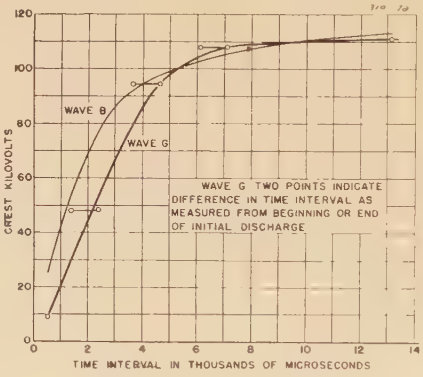
Recovery Voltages for Initial Discharges of Long Duration

The current waves of Figure 6 were produced with the surge generator of low capacity and series resistance consisting of a linear resistance in series with a number of arrester blocks. This provided a relatively low resistance at high currents but a very high resistance at low currents, thus tending to prolong the tail of the discharge. For the six-inch gap the current duration above the lower limit of recording sensitivity of 0.1 ampere is 1,000 microseconds. For the 11-inch gap it was only possible to prolong it for 600 microseconds, because more of the charge was needed in the gap at the instant of breakdown. This is shown by the higher initial crest in this case. The low current tails were recorded oscillographically with a nonlinear shunt having very high impedance at low currents and also by means of a high-speed photographic recorder.[4] (See Figure 6a.)

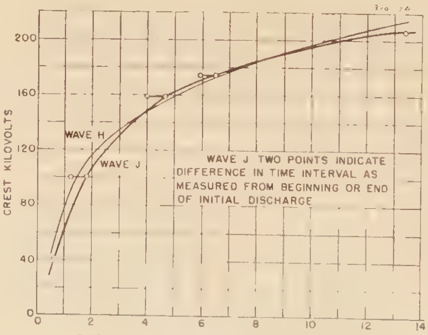
The critical breakdown curves obtained are shown in Figure 7 compared with those for discharges of short duration having the most nearly identical initial crest magnitudes. The data for the long-duration discharges are plotted for two different definitions of time interval; that defined as the time between the beginning of both the initial discharge and subsequently applied voltage and that as measured from the end of the initial discharge to the beginning of the applied voltage. It is interesting that when this shift is made in the points, they lie very close to the curves for the initial discharges of short duration except at time intervals where the initial current is still flowing at the application of voltage.

Breakdown When Current Is Still Flowing

As the time interval was lowered, it was found that although current continued to flow from the initial discharge, an appreciable voltage was still necessary in order to produce a second discharge. The breakdown was more erratic, but a definite critical value could be obtained. At 500 microseconds for the six-inch gap when about one ampere was flowing, the critical voltage was about five kilovolts and, for the 11-inch gap with about 0.4 ampere, 40 kv. The nature of the breakdown under these conditions is also interesting. Oscillograms of the two discharge currents and the second voltage are shown in Figures 8 and 9. During the period of breakdown the decrease in voltage is very gradual requiring several microseconds to drop to a relatively low voltage characterized by a fairly stable arc drop, but during the whole recorded period of voltage, it continues to decrease at a lesser rate. As higher voltages are applied, the period characterized by the more rapid drop in voltage becomes shorter until at twice the critical value it is practically instantaneous. The arc drop for the following period is also much lower, too low to record. As shown by the oscillograms of Figures 9b and 9c, the current in the second discharge rises slowly in the initial period corresponding to the slower rate of drop of voltage and increases more rapidly as the voltage drops more rapidly. The voltage and current relations are shown more clearly in Figure 10a which is a replot of Figures 9b and 9c superimposed. The character of the discharge current is quite different under these conditions than for normal breakdown. In the latter case, the path is first made partially conducting by initial leaders before the breakdown is completed by the return streamer process, which results in a high instantaneous flow of current and practically instantaneous drop in voltage. In this case, however, where considerable ionization exists because of the small flow of current, the transition from the low-conducting to high-conducting conditions is more gradual and uniform. The initial crest current characterizing the final breakdown of the gap is less pronounced and of longer duration. Comparison of the current in the second discharges of Figures 8 and 9 shows that



(a). 6-inch rod gap



(b). 11-inch rod gap

Figure 7. Comparison of voltage-recovery curves for short- and long-duration discharges

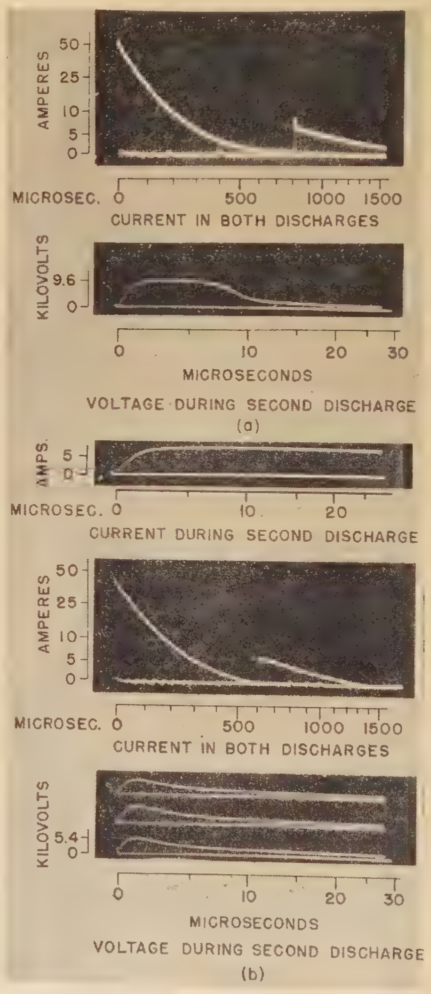


Figure 8. Current and voltage oscillograms when initial discharge is of long duration

6-inch rod gap. Wave G of Figure 6a

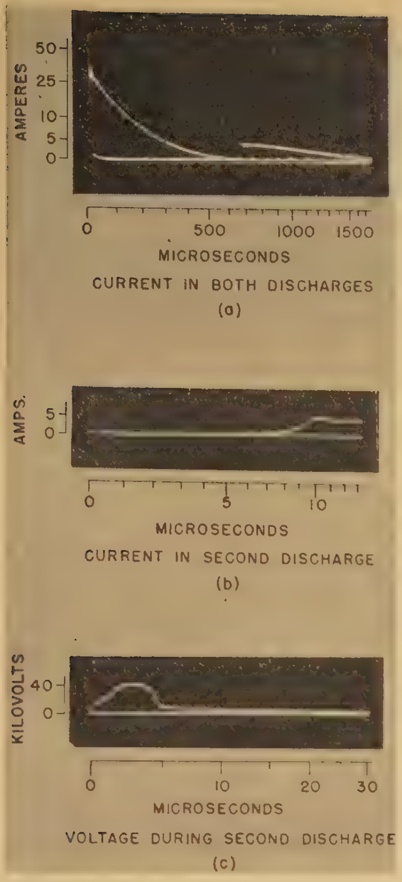


Figure 9. Current and voltage oscillograms when initial discharge is of long duration

11-inch rod gap. Wave *J* of Figure 6b

this initial crest increases with the time interval between discharges. The gradual transition of the arc path may have been influenced by the character of the voltage surge-generator circuit. The series resistance had to be fairly high, since only a small fraction of the potentiometer could be tapped off in producing the low discharge voltages. This could have produced appreciable regulation.

A measure of the effective impedance of the arc during the various stages can be obtained by determining the "effective" resistance from the current and voltage oscillograms. Such data are shown in Figure 10b. Synchronized simultaneous records were not obtained for each discharge; however, as shown by Figure 8b, the results could be duplicated with a good degree of consistency. It was found that for the initial discharges of Figure 6 the steady crest magnitude of the current in the second discharge was about five amperes for both gap spacings and essentially constant for a variation of time interval of as great as 300 microseconds. The arc resistance is, of course, quite high before the start of the second discharge, decreasing to about 1,800 ohms per foot for the 11-inch gap and 1,200 ohms per foot for the six-inch gap at the peak of the discharge current. Then it decreases more gradually. At the end of about 20 microseconds it is reduced by about 50 per cent. It is of interest to compare the arc under these transient conditions with that of stable arcs. After the arc has been established at the crest of the discharge current, the arc drop is about 9,000 volts per foot for the 11-inch gap and 6,000 volts per foot for the 6-inch gap. Although it is decreasing rapidly, it has not at the end of 20 microseconds dropped to anywhere near the normal value of 400 or 500 volts per foot, which is typical of steady-state conditions for arc currents of this magnitude.[5]

Photographic Study of the Arc Path

Photographic studies were made of the arc path by means of the high speed rotating camera and two still cameras mounted so as to photograph the arc from directions 90 degrees apart. Typical records are shown in Figure 11. About 250 microseconds is the shortest time interval for which a record could be obtained because the arc luminosity was too low to photograph for the very small currents resulting in the second discharge. As shown in Figure 11a, the path of the second discharge is not identical with that of the first for times as short as 250 microseconds. It diverges more and more as this time interval is increased. Up to about 2,000 microseconds the second discharge maintains the major bends of the first, but after about 5,000 or 6,000 microseconds it has departed entirely from the characteristics of the original path. Figures 11a and 11b show the second arc path to be more tortuous than the first. As shown by the sets of two still photographs, the second discharge follows a more or less winding path in a given cylindrical volume of air surrounding the original path, the radius of which increases with time as this volume of air with lower dielectric strength expands.

In Figure 12 an attempt was made to show the divergence of the two paths as a function of time. On this curve are plotted points representing the average of at least five records of the maximum three dimensional divergence of any part of the second arch path from the first. These data were obtained from simultaneous photographs taken with the two still cameras. For the short-duration discharges, divergence is more or less independent of the character of the initial discharge or of the gap spacing, and all the points lie close to a single curve for time intervals up to about 7,000 microseconds. After this, they diverge widely. For the long-duration discharges the divergence, when plotted as a function of time interval after the end of the first discharge, also conforms closely to the same mean curve except for the point at lowest time interval, for which current is still flowing. Then, although the arc path is still conducting current, the subsequent path blazed by the second discharge is not identical with that of the first which, as photographed, is probably the arc path formed during the initial high current portion of the discharge. During the longer period in which the lower current magnitude exists, the conducting core is expanding radially, and the current density is probably greatly decreased. This may contribute to some extent to the appreciable critical voltages when current is still flowing.

Mechanism of Recovery

The curve of Figure 12 gives a measure of the rate at which the conditions of low dielectric strength set up by the discharge in the arc path are expanding radially. The two principal conditions which may be contributing to the lower dielectric strength of this expanding region are high temperature and ionization. Both effects will propagate radially into successively larger volumes of air. As this heat and ion diffusion takes place, together with ion neutralization by recombination, the dielectric strength of the air should increase. Since both of these conditions are so interconnected, it has always been difficult to separate them for study. Browne,[6] however, has obtained data indicating that the ionization effect cannot be appreciable after the first 100 microseconds for short a-c arcs between

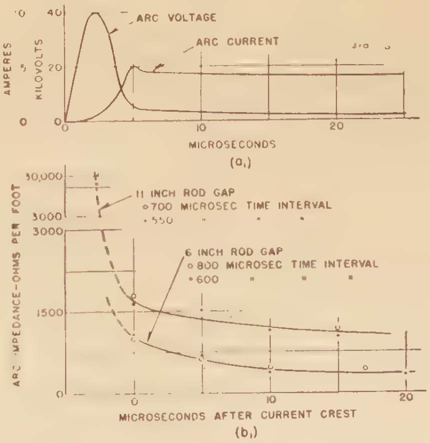
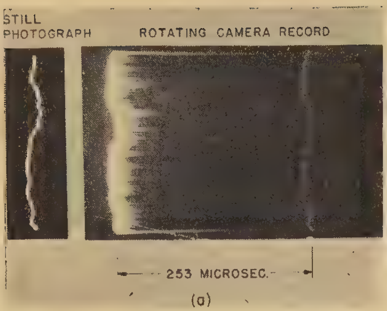
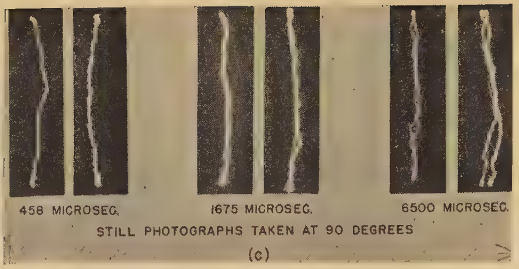


Figure 10. Voltage and current characteristic of discharges produced before or shortly after end of initial discharge





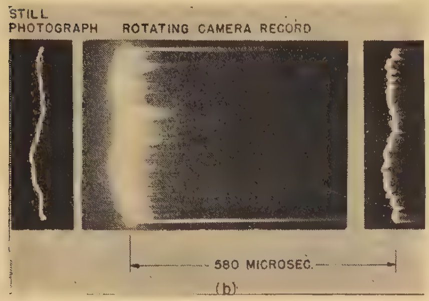


Figure 11. Multiple discharges showing relation between the two arc paths

11-inch rod gap

(a), (b). Wave *I* of Figure 2b
(c). Wave *H* of Figure 2b

parallel brass electrodes. Although this condition is quite different than for long arcs where electrode effects are much less, the consistency of breakdown and the character of the volt-time curves lead one to suspect that the temperature effect is also predominant for long arcs and short surges, at least for time intervals above 1,000 microseconds.

It is realized that turbulence and other factors insufficiently known by the authors make an analytical study of the problem difficult. However, some very approximate calculations have been made of temperature variation with time. Curve *A* of Figure 13 is a mean curve for the rate of voltage recovery of the six-inch rod gap. Curve *B* is a plot of temperature variation with time determined from curve *A* on the assumption of constant pressure throughout the air column and thus, that the dielectric strength of the air is inversely proportional to temperature. The starting point for the calculation was the critical breakdown voltage at normal room temperature. It is interesting that such a curve gives temperatures at the lower range of time intervals that correspond to those normal for arcs.

Any attempt at a rigorous calculation of rate of ion or temperature diffusion could not account for turbulence and would always give the highest ion density or temperature at the original arc core. Thus, successive discharges should always follow the same path blazed by the first. Such calculations for heat diffusion, assuming cylindrical symmetry and constant gas density, specific heat, and thermal conductivity, indicate rates of temperature drop of the same order of magnitude as given by curve *B* of Figure 13, but somewhat more rapid. It is probably the extreme condition of turbulence which produces localized regions of low dielectric strength of a more or less random distribution throughout a certain region whose radius is given by Figure 12.

Because of this, it was thought that perhaps the only analysis which was warranted was merely the very crude one based on the assumption that the heat produced in the original arc column resides at any time interval in a cylindrical column of air given by the radius of Figure 12 for that time interval and that the temperature in the column is uniform. Curve *C* of Figure 13 was calculated on this basis, starting with a temperature of 4,000 degrees Kelvin and a radius of 0.13 inch at a time interval of 250 microseconds. It conforms quite closely to curve *B*. This is not necessarily confirmation of the accuracy of either curve, but it is believed to indicate that the temperature of the air column alone can account for the rate of voltage recovery.

Mechanism of Natural Lightning

Photographs of natural lightning taken with the Boys camera or similar devices imparting the time element to the record indicate that all components of a multiple stroke follow the same path except in rare cases when a component discharge will follow the original path part way to earth, then depart entirely from it. A record of a typical multiple stroke obtained in the Westinghouse investigation is shown in Figure 14. However, deviation of a fraction of an inch or even several inches cannot be distinguished on such records and probably do exist.

The rates of voltage recovery which have been obtained indicate that the time intervals between components can be high enough (ranging from 0.0005 to 0.5 second[9]) that, unless low current flows for an appreciable portion of the intervening period, the subsequent discharge should not always follow the same path. From evidence provided by strokes to very tall objects, Bruce and Golde[10] have concluded that current must flow continuously between components. However, four records have been obtained with the photographic surge-current recorder[4] of multiple strokes to low objects. Two of these records are shown in Figure 15. This device is capable of measuring currents as low as 0.1 ampere, and for all of the records there is an appreciable period between at least two components in which

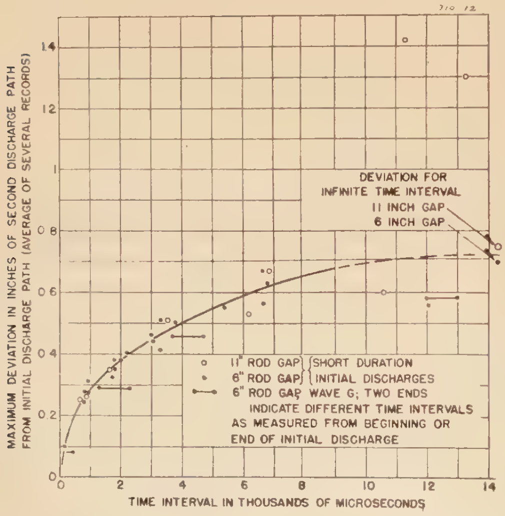


Figure 12 (left). Curve showing variation of second arc from initial arc path as function of time interval between discharges

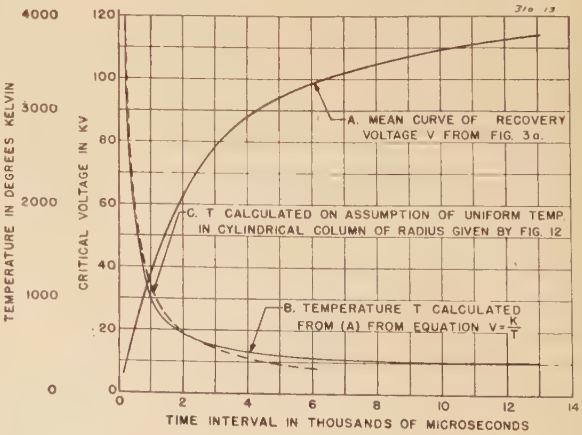


Figure 13 (right). Calculated curves of variation of arc-path temperature with time after initial discharge

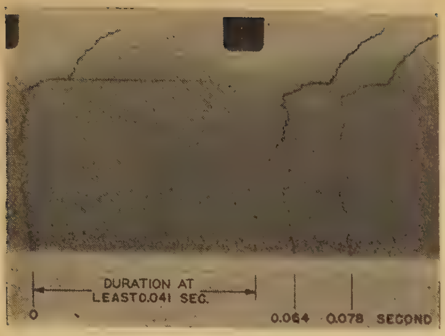


Figure 14. Record of multiple stroke to open ground

no current was measured. It is interesting that none of these periods has exceeded about 23,000 microseconds. The extrapolated voltage-recovery curves of Figure 3b indicate that, for stroke currents of the general character studied, 30,000 to 40,000 microseconds should be about the upper time-interval limit for zero current between successive components. Otherwise, the dielectric strength of the original stroke channel will have sufficiently recovered that the subsequent discharge might take a totally different path. However, some of the strokes which have been measured[9] have continuing currents of such high magnitude and duration that their rate of recovery may be appreciably lower.

Records of strokes, such as Figure 14, show extreme fluctuations of luminosity as a characteristic condition in the stroke channel. The long-duration stroke component in Figure 15 shows a similar wide range of fluctuation of current which is probably associated with such luminosity variations.

Comparison With Rate of Dielectric Recovery for Power Arcs

The rapid rate of recovery and small dependence on current magnitude for surges of short duration are in considerable variance with results that have been found for power arcs. Rates of recovery for 60-cycle arcs have been determined only for very short gaps (one to ten millimeters) where electrode effects and field considerations are quite different.[6,7] These data for currents of the order of five to ten amperes show rates of recovery as rapid as those for short-duration surges. However, as the power current is increased, the rate of recovery drops rapidly. For currents above 100 amperes such data indicate that an appreciable fraction of a second is required for recovery to anywhere near the original strength of the gap.

Evidence that this is also the case for long arcs is provided by tests of Griscom and Torok[8] in which the probability of restriking was determined for gap spacings of the order of 10 to 100 inches. Arcs with currents ranging from 800 to 1,500 amperes were started with fuse wire and allowed to continue for a period of about nine cycles, which should have been sufficient to eliminate the effect of the vaporized wire. The arc was interrupted and voltage reapplied after various time intervals to determine the probability of restriking. The voltages were those of transmission lines with conductor spacings equal to the gap spacings and were thus about 25 per cent of the normal gap breakdown voltage. Nevertheless, the probability of restriking was found to be appreciable for periods as long as five or six cycles.

Except for the time element introduced by multiple strokes of appreciable duration, the character of the power-follow current, since it produces such slower rates of recovery than the initiating lightning flashover, should be the principal factor governing circuit-reclosing times. Reclosing times should thus be based on the probability of the duration of multiple strokes[9] and the probability of restriking for power arcs.

Probability of Lightning Flashover Producing Power Follow

The high rate of dielectric recovery for surge-current discharges in air gaps suggests that, for lines where flashover will occur in air at atmospheric pressure, an appreciable percentage of the flashovers that do not cause an outage may be lightning flashovers that produce a disturbance of very short duration. That this is the case is shown by an analysis of system recovery voltage conditions for lightning flashover. Stroke components of sufficiently short duration can cause flashover on a certain portion of the normal conductor voltage cycles and produce a current zero occurring in a relatively short time. The current flow in the gap will be such that the rates of dielectric recovery are comparable to those of Figure 3 and lying above the system recovery volt-time curve.

There are two conditions for which such rapid arc extinction will occur. The first of these is illustrated in Figure 16a for which the lightning flashover occurs at a time (T) before and close to the time of normal zero system voltage. If the duration the of lightning discharge were negligible, the fundamental-frequency fault current would have, as shown in Figure 16a, an initially low magnitude followed quickly by a current zero. The magnitude and duration of this current and the magnitude of the fault recovery voltage increase with the time (T). The recovery voltage curves as a function of (T) have been determined with the a-c network calculator[11] for a typical simple system having a natural frequency of 1,000 cycles per second. This corresponds to about a 40-mile line. The system recovery voltage curves are shown in Figure 16b compared with the dielectric-recovery curves for two ratios of insulation level to normal crest system voltage, four times normal and six times normal. As shown by this figure, the maximum time T for which the system recovery curve lies below the dielectric-recovery curve is about 1,100 and 1,700 microseconds for the respective insulation levels. This condition can be produced by stroke currents with polarity either the same or opposite to the initial normal frequency fault current. It can thus occur on either half-cycle. If the duration of the lightning discharge does not tend to shift the normal current zero, these recovery curves can be applied to surges of appreciable duration. A calculation of the probability of flashovers not producing power follow was made on the conservative assumptions that they could be produced by single-component strokes only, having durations less than the time from the instant of flashover to the time of normal-frequency current zero. With the data available on the probability of multiple strokes and stroke-component durations[9] such an analysis indicates probabilities of flashovers not producing power follow of five and nine per cent.

Lightning flashover occurring shortly after normal system voltage zero will produce a current zero in a short time if it is the opposite polarity to the normal-frequency fault current. Thus on the assumption that strokes are predominantly negative there is a certain period on every other half-cycle when this condition can take place. Neglecting the effect of the initiating transient, the system recovery voltage curve, as a function of the time of current zero after normal system voltage zero can be estimated for this case by considering its crest to be twice the normal system voltage at the instant of current zero. It reaches crest in 500 microseconds for the system considered. Such an assumption indicates that the percentage of strokes which will not produce power follow are three and five, respectively, for the two insulation levels. This gives a total of 8 and 14 per cent of strokes causing flashover with disturbances of only short duration. The

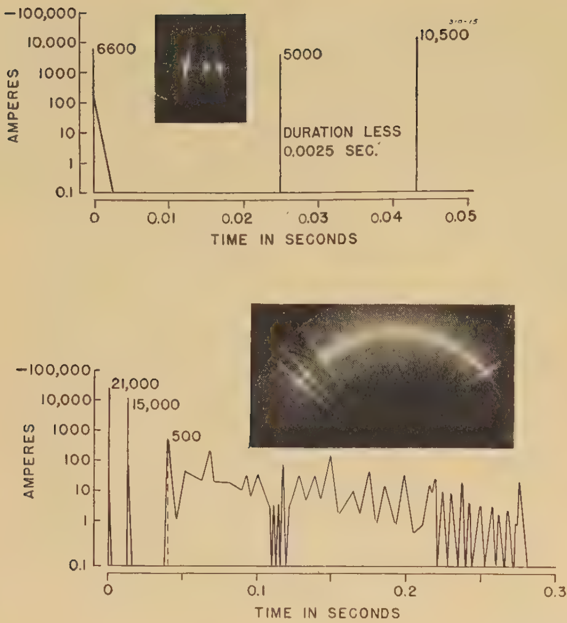
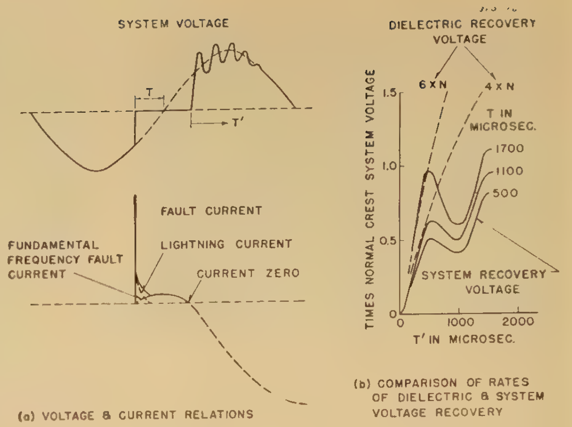


Figure 15 (left). Current in multiple lightning strokes to 85-foot fire towers as obtained with photographic surge-current recorder[4]

Figure 16 (right). Condition for lightning flashover on transmission line causing only short-duration disturbance

Dielectric-recovery curves obtained from Figure 3. $4 \times N$ represents air-gap insulation level four times normal crest system voltage



(a) VOLTAGE & CURRENT RELATIONS

(b) COMPARISON OF RATES OF DIELECTRIC & SYSTEM VOLTAGE RECOVERY

probability of rapid arc extinction will increase with the ratio of the insulation level of the line to the line voltage and with the length of the line or reduction in its natural frequency.

Data[12] have been obtained showing the percentage of system disturbances whose duration is one cycle or less. These data show an average of about eight per cent of such disturbances for steel tower lines and about 20 per cent for wood-pole lines. These short-duration disturbances are probably of the nature of those discussed above. Good agreement is shown for steel lines where the flashover path is confined entirely to air. The higher percentage for wood-pole lines confirms other data indicating that wood insulation has greater extinguishing power than air alone. Additional data[9] available on the ratio of flashovers to outages on steel tower lines indicate a range of from 15 to 35 per cent for the proportion of total flashovers not producing outages. Thus it is indicated that about half of the flashovers not causing a fault are these very short-duration disturbances.

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TRANSACTIONS SECTION

Preprint of Corresponding Pages From the Current Annual AIEE Transactions Volume

Any discussion of these papers will appear in the June 1943 Supplement to Electrical Engineering—Transactions Section

Formulas for the Calculation of the Inductance of Linear Conductors of Structural Shape

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THE mechanical and electrical advantages stemming from the use of linear conductors of structural shape* are so marked that, at present, most busses for heavy current duty are constructed of such conductors. Thus, standard designs of single and polyphase busses utilize strap conductors of full rectangular cross section, tubular conductors of hollow rectangular cross section, or ventilated conductors of open hollow rectangular cross section—these last built up of two slightly separated channels or four slightly separated angles placed flange to flange, or of multiples of four thin strap conductors. Again, limited construction of a somewhat more specialized type of bus has resulted from Arnold's[1] discovery that minimum eddy-current losses are attained in a single-phase circuit if, of the conductors commercially available, the bus is constructed of two properly chosen channels placed back to back. Finally, the use, in shielded busses, of conductors of T and I cross section is not unknown.

Yet, despite the use of these various types of conductors, explicit formulas for calculating the inductance—and thence the reactive voltage drop, often the determining factor in the design of a heavy current bus—seemingly are available for only the first two types of conductors: those of full and those of hollow rectangular cross section. Papers by Arnold,[2] Carlson,[3] Darling,[4,5] Dwight,[6,7] Gray,[8] Grover,[9] Higgins,[10–12] Levy,[13] Maxwell,[14] Orlich,[15] Rosa,[16–19] Roth,[20] Schurig,[21] Silsbee,[22] Steidinger,[23] Sumec,[24] and others[25,26] contain formulas, curves, and tables that facilitate calculation of the inductance of a single-phase or of a polyphase bus comprised of identical conductors having parallel coplanar axes and parallel-sided full rectangular cross sections (if corresponding sides of two rectangular areas are parallel, but the areas are otherwise arbitrarily located, these areas are termed "parallel-sided"). And papers by Darling,[4,5] Dwight and Wang,[27] and Higgins,[10–12] contain formulas, curves and tables for calculating the inductance of a single-phase or of a polyphase bus comprised of identical conductors having parallel coplanar axes and parallel-sided hollow rectangular cross sections. But, seemingly, there have not been published explicit formulas for calculating the inductance of a bus comprised of full or hollow parallel-sided rectangular conductors arranged other than as just described; nor of a bus comprised of one of certain currently employed arrangements of linear conductors of L, T, [, or I cross section.

Such formulas are derived in this paper, it being assumed that the conductors are nonmagnetic, are of such length that end effects are negligible, are right-cornered, and carry currents uniformly distributed over their cross sections.

Of these four conditions, the first and second commonly are satisfied in practice. The third, however, though essential to tractable analysis, is not always true in practice. For example, strap conductors of full rectangular cross section are usually right-cornered, whereas conductors of hollow rectangular cross section commonly have rounded edges; again, channels built up of strap conductors are usually right-cornered, whereas channels drawn or rolled in one piece usually have rounded edges. However, error introduced by neglecting the rounded edges is usually small; moreover, where necessary the increase in inductance can be approximated by use of semiempirical formulas derived by Dwight and Wang.[27] Finally, neglect of skin and proximity effects is also essential to tractable analysis. However, the effect of these phenomena on the magnitude of the inductance is on the safe side, for the inductance decreases as the frequency is increased; moreover, at power frequencies and normal bus spacings this reduction in inductance is practically negligible (see, for example, the discussion following reference 27 or the experimental results published in reference 2).

If these four postulates are granted, derivation of the desired formulas, though lengthy, is not particularly difficult. First, the formula for the geometric mean distance between two *arbitrarily located* parallel-sided rectangular areas is derived by direct integration (sections I and II). Next, this formula and the fundamental theorem of geometric mean distance theory are conjoined to obtain formulas for the geometric mean distance between two arbitrary configurations comprised of parallel-sided rectangular areas; whence general formulas for the inductance of conductors of such cross sec-

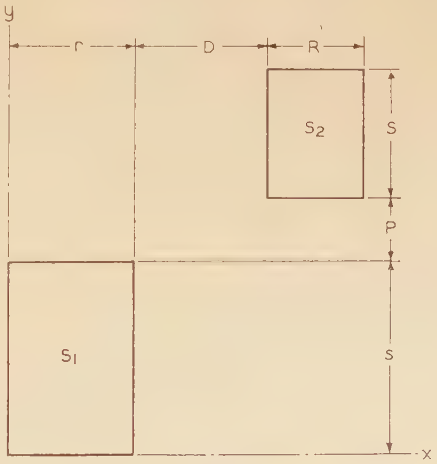

Figure 1

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* As conductors of full or annular circular cross section have been treated exhaustively elsewhere, they are not considered in this paper.

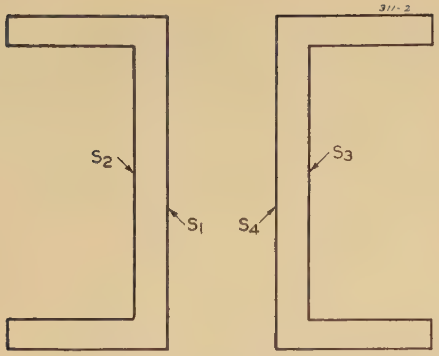

Figure 2

tion follow from application of well-known theory (sections III and IV). Finally, since the category just described embraces conductors of the type under discussion, distinct formulas for the inductance of specific types and arrangements of conductors can be deduced from these general formulas as desired (section V).

I. The Geometric Mean Distance Between Parallel-Sided Rectangles

The geometric mean distance D_{12} of a rectangular area S_1 from a second arbitrarily located rectangular area S_2 is defined by

$$S_1S_2 \log D_{12} = \int_{S_2}\int_{S_1} \log[|X-x|^2+|Y-y|^2]^{1/2} dS_1 dS_2 \quad (1)$$

(x, y) being the co-ordinates of a point in S_1 and (X, Y) those of a point in S_2.

If the rectangular areas are parallel-sided, are located as in Figure 1, and if P and D are *positive* (the necessity and the significance of this restriction are discussed in section II), we have by equation 1

$$RSrs \log D_{12} = \int_{D+r}^{D+R+r}\int_{P+s}^{P+S+s}\int_0^r\int_0^s \log[|Y-y|^2+|X-x|^2]^{1/2} dy\,dx\,dY\,dX \quad (2)$$

Evaluation of the quadruple integral—the details of integration are contained in the appendix—yields

$$RSrs \log D_{12} = -(25/12)RSrs - (1/24)\sum_{i=1}^{4}\sum_{j=1}^{4}(-1)^{i+j}K(A_i, B_j) \quad (3)$$

wherein

$$K(A_i, B_j) = (A_i^4-6A_i^2B_j^2+B_j^4) \times \log(A_i^2+B_j^2)^{1/2} - 4A_iB_j^3\tan^{-1}(A_i/B_j) - 4A_i^3B_j\tan^{-1}(B_j/A_i) \quad (4)$$

and

$$A_1 = D+R+r;\ A_2 = D+R;\ A_3 = D;\ A_4 = D+r \quad (5)$$

$$B_1 = P+S+s;\ B_2 = P+S;\ B_3 = P;\ B_4 = P+s \quad (6)$$

Certain variants of equation 3 and of equation 4 are of value. Recalling that

$$\tan^{-1}(A_i/B_j) + \tan^{-1}(B_j/A_i) = \pi/2 \quad (7)$$

providing $A_i,\ B_j \geq 0$ we have by equations 4 and 7

$$\begin{aligned} K(A_i, B_j) &= (A_i^4-6A_i^2B_j^2+B_j^4)\times \log(A_i^2+B_j^2)^{1/2}+4A_iB_j(A_i^2-B_j^2)\tan^{-1}(A_i/Bj)-2\pi A_i^3B_j \\ &= (A_i^4-6A_i^2B_j^2+B_j^4)\times \log(A_i^2+B_j^2)^{1/2}+4A_iB_j(B_j^2-A_i^2)\times \tan^{-1}(B_j/A_i)-2\pi A_iBj^3 \end{aligned} \quad (8)$$

As by equations 5 and 6

$$2\pi A_i^3\sum_{j=1}^{4}(-1)^jB^j = 2\pi B_j^3\sum_{i=1}^{4}(-1)^iA_i = 0 \quad (9)$$

providing *both P and D are positive* as postulated above, substitution of equation 8 in equation 3 yields

$$RSrs \log D_{12} = -(25/12)RSrs - (1/24)\sum_{i=1}^{4}\sum_{j=1}^{4}(-1)^{i+j}M(A_i, B_j) \quad (10)$$

wherein

$$M(A_i, B_j) = (A_i^4-6A_i^2B_j^2+B_j^4)\times \log(A_i^2+B_j^2)^{1/2}+4A_iB_j(A_i^2-B_j^2)\times \tan^{-1}(A_i/B_j) \quad (11)$$

By virtue of the equivalent forms of the right-hand members of equation 8, and similarly those of equation 11, we obtain the useful identities

$$K(A_i, B_j) = K(B_j, A_i);\quad M(A_i, B_j) = M(B_j, A_i) - 2\pi A_iB_j(B_j^2-A_i^2) \quad (12)$$

II. Generalization of P and D

The reason for the restriction, in section I, of P and D to positive values is evidenced by the following remarks on integration in the variable Y. If P and D are positive, we have $Y>y$; whence $(Y-y) \equiv |Y-y|$ and $d(Y-y)/dY \equiv d|Y-y|/dY$ over the domain of integration in Y; and integration in Y with either $(Y-y)$ or $|Y-y|$ in the integrand yields the same value. But if P and D can, separately, take on either positive *or* negative values, then over the range of integration in Y it may be that $(Y-y)$ takes on both positive and negative values. If so, $d|Y-y|/dY=1$ if $Y>y$; but $d|Y-y|/dY=-1$ if $Y<y$; $|Y-y|=(Y-y)$ if $Y>y$, but $|Y-y|=-(Y-y)$ if $Y<y$; and integration with $(Y-y)$ in the integrand *may* yield a value different from that obtained with $|Y-y|$ in the integrand: for example,

$$\int_0^{2a}(Y-a)dY = 0, \text{ but } \int_0^{2a}|Y-a|dY = a^2;$$

$$\text{contrariwise, } \int_0^{2a}|Y-a|^2dY = \int_0^{2a}(Y-a)^2dY = 2a^3/3$$

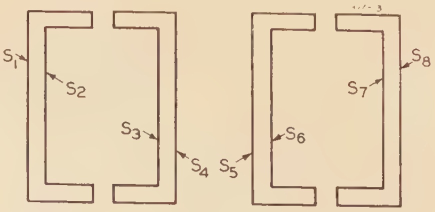

Figure 3

When with this point of analysis in mind the quadruple integral in the right-hand member of equation 2 is evaluated for each possible relative position of the two parallel-sided rectangular areas—the details of integration are discussed in the appendix—it is found that in each case equation 3 yet results *providing that in equation 3 A_i and B_j now indicate the following* (of which, obviously, equations 5 and 6 are special cases)

$$A_1 = |D+R+r|;\ A_2 = |D+R|;\ A_3 = |D|;\ A_4 = |D+r| \quad (13)$$

$$B_1 = |P+S+s|;\ B_2 = |P+S|;\ B_3 = |P|;\ B_4 = |P+s| \quad (14)$$

Further, as equation 7 is satisfied by these generalized values of A_i and B_j, equation 8 is likewise generally valid. Contrariwise, however, equations 10 and 11 are valid only if *both P and D are positive:* nevertheless, as this case frequently occurs in practice, these latter equations are most useful.

With the restriction on P and D thus removed, the foregoing equations afford the solutions to a host of electromagnetic problems (on the calculation of self-inductance and mutual inductance, reactance, line voltage drop, short-circuit bus stresses, and the like) that confront the engineer concerned with the design of single or polyphase busses composed of parallel linear conductors with axes not necessarily coplanar and with right cross sections comprised of arbitrary configurations of parallel-sided rectangles.

III. General Formulas for the Inductance and Reactance of Busses

Though solutions to problems of the type just mentioned can be obtained by direct use of the formulas derived in sections I and II, a considerable and very desirable simplification of the numerical computation inherent to the actual design of a given bus can be effected if these formulas are conjoined with certain very general expressions to be derived by repeated use of the cardinal theorem of geometric mean-distance theory. This theorem states that:

If $S_1, S_2, \ldots, S_m$ are the components of a total area S; if $D_1, D_2, \ldots, D_m$ are their respective geometric mean distances from a second area S'; and if D is the geometric mean distance between S and S', then

$$SS' \log D = S_1S' \log D_1 + S_2S' \log D_2 + \ldots + S_mS' \log D_m \quad (15)$$

A direct corollary of this theorem is:

Let S' be comprised of component areas $S_1', S_2', \ldots, S_n'$. Then as by equation 15

$$S_1S' \log D_1 = S_1S_1' \log D_{11} + S_1S_2' \log D_{12} + \ldots + S_1S_n' \log D_{1n} \quad (16)$$

and as $S_2S' \log D_2, \ldots, S_m S' \log D_m$ can be expressed similarly, we have on substituting equation 16 and like forms in equation 15

$$SS' \log D = \sum_{i=1}^{m} (S_iS_1' \log D_{i1} + S_iS_2' \times \log D_{i2} + \ldots + S_iS_n' \log D_{in}) = \sum_{i=1}^{m} \sum_{j=1}^{n} S_iS_j \log D_{ij} \quad (17)$$

We note for later use that if each of the two cross sections is comprised of identical components numbered alike, $m=n$ and $S_{ij} \log D_{ij} = S_{ji} \log D_{ji}$.

Alternatively, if the components of S' are numbered from $m+1$ to $m+n$ instead of from 1 to n, we obtain from equation 17

$$SS' \log D = \sum_{i=1}^{m} \sum_{j=m+1}^{m+n} S_iS_j \log D_{ij} \quad (18)$$

For determining the self geometric mean distance of an area to itself equation 17 is preferable; for determining the geometric mean distance between two distinct areas equation 18 is preferable.

Granted the four postulates stated above, equations 15 to 18 inclusive facilitate derivation of convenient and compact expressions for the inductance of circuits comprised of parallel linear conductors of uniform but otherwise arbitrary cross section. Thus, invoking fundamental electromagnetic principles, if S_a and S_b are the cross sections of two conductors comprising a single-phase circuit carrying a current I, we have for the vector potential $A(X, Y)$ at a point (X, Y)

$$A = \text{constant} - 2 \iint_{S_a} w_a \log [|X-x|^2 + |Y-y|^2]^{1/2} dxdy - 2 \iint_{S_b} w_b \times \log [|X-x|^2 + |Y-y|^2]^{1/2} dxdy \quad (19)$$

wherein w_a and w_b are, respectively, the current densities in S_a and S_b, taken positive for current in one direction and negative for current in the other direction (hence, w_aw_b is a negative quantity, w_a^2 and w_b^2 positive quantities, and $I = w_aS_a = -w_bS_b$). The energy in the magnetic field associated with the circuit is

$$W = {}^1/_2 \iint_{S_a} A(X, Y) w_a dXdY + {}^1/_2 \iint_{S_b} A(X, Y) w_b dXdY \quad (20)$$

Substitution of equation 19 in equation 20, and then of equation 1 in the resulting expression yields

$$W = -(2w_aw_bS_aS_b \log D_{ab} + w_a^2S_a^2 \log D_{aa} + w_b^2S_b^2 \log D_{bb}) = I^2 \log (D_{ab}^2/D_{aa}D_{bb}) \quad (21)$$

Finally, recalling that $W = LI^2/2$, we have from equation 21

$$L = -(2/I^2)(2w_aw_bS_aS_b \log D_{ab} + w_a^2S_a^2 \times \log D_{aa} + w_b^2S_b^2 \log D_{bb}) = 2 \log D_{ab}^2/D_{aa}D_{bb} \quad (22)$$

If each of the two conductors is comprised of several component conductors connected in parallel, m comprising that with cross section S_a and n that with cross section S_b, substitution of appropriate forms of equations 17 and 18 in equation 22 yields

$$L = -(2/I^2) \Big[\sum_{i=1}^{m} \sum_{j=m+1}^{m+n} 2w_iw_jS_iS_j \log D_{ij} + \sum_{i=1}^{m} \sum_{j=1}^{m} w_iw_jS_iS_j \log D_{ij} + \sum_{i=m+1}^{m+n} \sum_{j=m+1}^{m+n} w_iw_jS_iS_j \log D_{ij} \Big] = -(2/I^2) \sum_{i=1}^{m+n} \sum_{j=1}^{m+n} w_iw_jS_iS_j \log D_{ij} \quad (23)$$

If we have two distinct circuits comprised of $p+q$ conductors *in toto*, p comprising one circuit and carrying current I_p, q comprising the other circuit and carrying current I_q, a similar analysis yields for the energy associated with the two circuits

$$W = -\sum_{i=1}^{p+q} \sum_{j=1}^{p+q} w_iw_jS_iS_j \log D_{ij} \quad (24)$$

Recalling that

$$W = MI_pI_q + L_pI_p^2/2 + L_qI_q^2/2 \quad (25)$$

substitution of equation 24 and of appropriate forms of equation 23 in equation 25 yields for the mutual inductance M

$$M = -(1/I_pI_q) \Big[\sum_{i=1}^{p+q} \sum_{j=1}^{p+q} w_iw_jS_iS_j \log D_{ij} - \sum_{i=1}^{p} \sum_{j=1}^{p} w_iw_jS_iS_j \log D_{ij} - \sum_{i=1}^{q} \sum_{j=1}^{q} w_iw_jS_iS_j \log D_{ij} \Big] \quad (26)$$

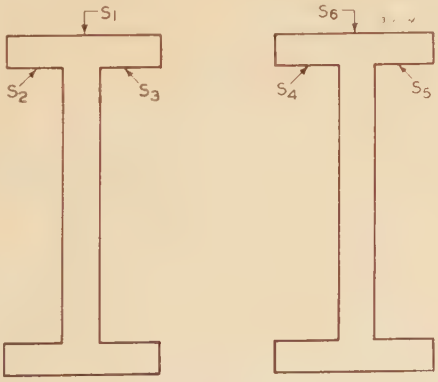

Figure 4

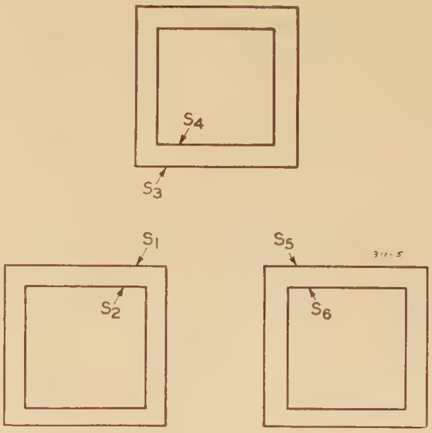

Figure 5

With regard to the physical quantities mentioned to this point, all units are in the absolute system: linear dimensions in centimeters, current I in abamperes, energy W in ergs per centimeter of line length, self inductance L and mutual inductance M in abhenrys per centimeter of line length. The 60-cycle reactance X in ohms per thousand feet of line length is obtained by multiplying a value of inductance by 11.48×10^{-3}.

IV. Polyphase Systems

In calculating the performances of polyphase systems it is necessary to know the reactance voltage drops on individual conductors. The well-known formulas for calculating the reactance drops are easily derived. In an n-phase system comprised of n distinct conductors, one conductor, say a, can be considered as carrying current I_a (the currents are expressed as complex numbers), and the remaining $n-1$ conductors can be considered as connected in parallel and carrying the return current $-I_a=(I_b+I_c+\ldots+I_n)$. Accordingly, as the energy associated with conductor a is given by $W_a={}^1/_2\int Aw_a dS_a$, we obtain by selection of the appropriate terms from equation 22 that the associated inductance, $L_a=$ (reactance drop on conductor a)$/(2\pi f I_a)$, is

$$L_a=-(2/I_a{}^2)(w_a{}^2S_a{}^2\log D_{aa}+w_aS_a\sum_{i=b}^{n}w_iS_i\log D_{ai})$$
$$=-2\sum_{i=a}^{n}(I_i/I_a)\log D_{ai}\text{ abhenrys per centimeter of conductor length}\quad\mathbf{(27)}$$

By equation 27 the 60-cycle reactance is

$$X_a=-240\pi\sum_{i=a}^{n}(I_i/I_a)\log D_{at}\text{ abohms per centimeter of conductor length}$$
$$=0.0528\sum_{i=a}^{n}(I_i/I_a)\log_{10}(1/D_{at})\text{ ohms per thousand feet of conductor length}$$
$$=0.2794\sum_{i=a}^{n}(I_i/I_a)\log_{10}(1/D_{at})\text{ ohms per mile of conductor length}\quad\mathbf{(28)}$$

If the individual conductors are composite, a composed of a' conductors numbered from 1 to a', b of b' conductors numbered from $a'+1$ to $a'+b'$, and correspondingly for the others, we obtain by substitution of appropriate forms of equation 16 in equation 27

$$L_a=-(2/I_a{}^2)\left[\sum_{i=1}^{a'}\sum_{j=1}^{a'}w_iw_jS_iS_j\log D_{ij}+\sum_{i=1}^{a'}\sum_{j=a'+1}^{a'+b'}w_iw_jS_iS_j\log D_{ij}+\ldots+\sum_{i=1}^{a'}\sum_{j=m'+1}^{m'+n'}w_iw_jS_iS_j\log D_{ij}\right]$$
$$=-(2/I_a{}^2)\left[\sum_{1=i}^{a'}\sum_{j=1}^{a'+\ldots+n'}w_iw_jS_iS_j\log D_{ij}\right]\text{ abhenrys per centimeter length}\quad\mathbf{(29)}$$

Corresponding formulas for the reactance are obvious from equation 28.

V. Some Illustrative Examples

The various parts of example 1 are advanced as corroborative of the fundamental formula given in equation 3, wherein A_i and B_j are as defined in equations 13 and 14. The succeeding examples are advanced as demonstrative of the application of the content of sections I to IV to the design of standard busses of various types.

EXAMPLE 1

Part A. The two rectangular areas of Figure 1 are to be considered as identical and superimposed. For this case: $r=R$; $s=S$; $D=-r$; $P=-s$. Then equations 13 and 14 yield: $A_1=A_3=r$; $A_2=A_4=0$; $B_1=B_3=s$; $B_2=B_4=0$. Whence, recalling that $\lim_{x\to 0} x^4\log x=0$, substitution in equation 3 as indicated yields Maxwell's well-known formula for the geometric mean distance of a rectangular area to itself

$$\log D_{12}=\log(r^2+s^2)^{1/2}-(r^2/6s^2)\log[(r^2+s^2)/r^2]^{1/2}-(s^2/6r^2)\log[(r^2+s^2)/s^2]^{1/2}+(2s/3r)\tan^{-1}(r/s)+(2r/3s)\tan^{-1}(s/r)-25/12\quad\mathbf{(30)}$$

Part B. The two rectangular areas of Figure 1 are to be considered as identical squares ($R=S=r=s$), so located that $P=D=0$. Then equations 13 and 14 yield: $A_1=B_1=2r$; $A_2=B_2=A_4=B_4=r$; $A_3=B_3=0$. Whence, substitution in equation 3, as indicated, yields the known formula due to Rosa (reference 16, page 13)

$$\log D_{12}=\log r+3\log 2-(7/12)\log 5-4\tan^{-1}(1/2)+\pi-(25/12)$$
$$=\log r+0.34427+\quad\mathbf{(31)}$$

Part C. In similar fashion, if we take $r\neq R$, $s\neq S$, $D=D$, $P=-(s+S)/2$, we obtain Gray's well-known formula for the geometric mean distance between two dimensionally different, symmetrically located rectangles (reference 16, page 6); if we consider two identical squares ($R=S=r=s$), so located that $D=0$ and $P=P$, we obtain the known formula due to Rosa (reference 16, page 12); and if we take $r\neq R$, $s\neq S$, $D=-(r+R)/2$, $P=-(s+S)/2$, we obtain the writer's formula for the geometric mean distance between two symmetrically superimposed, dimensionally different rectangles (reference 11, page 1049). Accordingly, as verification of the analysis of sections I and II we have that equation 3 yields the known formulas for those special cases of the geometric mean distance between two parallel-sided rectangles that have been treated hitherto.

EXAMPLE 2

A single-phase bus is comprised of two identical channels placed back to back (Figure 2). To calculate the inductance per-unit length of bus:

Each of the two channels can be considered as comprised of two component conductors: one of full rectangular cross section wherein current exists in the same direction as in the channel, the other of smaller rectangular cross section having current in the opposite direction. Hence: $S_1=S_4$; $S_2=S_3$; $w_1=-w_2=w_3=-w_4=I/(S_1-S_2)$. Substituting in equation 23 yields

$$L=-(2/I^2)\sum_{i=1}^{4}\sum_{j=1}^{4}w_iw_jS_iS_j\log D_{ij}\text{ abenrys per centimeter of bus length}\quad\mathbf{(32)}$$

Substituting in equation 32 as indicated and collecting terms, we have

$$L=-[4/(S_1-S_2)^2][S_1{}^2(\log D_{11}-\log D_{14})+S_2{}^2(\log D_{22}-\log D_{23})-2S_1S_2(\log D_{12}-\log D_{13})]\quad\mathbf{(33)}$$

Corroboratively, if $S_2=S_3=0$ we have conductors of full rectangular cross section, and equation 33 reduces to the known formula $L=4\log(D_{14}/D_{11})$.

The six geometric mean distances in the right-hand member of equation 33 can be calculated from equation 3. Commonly, though, the value of the self geometric mean distance of a rectangle or of the geometric mean distance between two rectangles, symmetrically and externally located relative to one another, can be obtained—and usually more quickly than by calculation—from tables contained in certain of the references, especially reference 9. For other than this restricted class, though, equation 3 must be used perforce.

EXAMPLE 3

A single-phase bus is comprised of two identical open hollow rectangular conductors, each conductor consisting of two slightly separated channels placed flange to flange (Figure 3). To calculate the inductance per-unit length of bus:

Each of the two conductors can be considered as comprised of four component conductors: two of rectangular cross section wherein current exists in the same direction as in the split conductor; the remaining two of smaller rectangular cross section having current in the opposite direction. Hence: $S_1=S_4=S_5=S_8$; $S_2=S_3=S_6=S_7$; $w_1=-w_2=-w_3=w_4=-w_5=w_6=w_7=-w_8=I/2(S_1-S_2)$. By equation 23 the inductance of the bus is

$$L=-(2/I^2)\sum_{i=1}^{8}\sum_{j=1}^{8}w_iw_jS_iS_j\log D_{ij}\text{ abhenrys per centimeter of bus length}\quad\mathbf{(34)}$$

Substituting in equation 34 as indicated and collecting terms yields

$$L=-[1/(S_1-S_2)^2][S^2(2\log D_{11}+2\log D_{14}-2\log D_{15}-\log D_{18}-\log D_{45})+S_2{}^2(2\log D_{22}+2\log D_{23}-2\log D_{26}-\log D_{27}-\log D_{36})-2S_1S_2(2\log D_{12}+2\log D_{13}-2\log D_{16}-\log D_{17}-\log D_{35})]\text{ abhenrys per centimeter of bus length}\quad\mathbf{(35)}$$

EXAMPLE 4

A single-phase line is comprised of two identical conductors of I cross section (Figure 4). To calculate the inductance per unit of bus length:

Each of the two conductors can be considered as comprised of three component conductors: one of rectangular cross section wherein current exists in the same direction as in the conductors of I cross section, the remaining two of smaller rectangular cross section having current in the opposite direction. Hence: $S_1=S_6$; $S_2=S_3=S_4=S_5$; $w_1=-w_2=-w_3=w_4=w_5=-w_6=I/(S_1-2S_2)$. By equation 23 the inductance of the bus is

$$L=-(2/I^2)\sum_{i=1}^{6}\sum_{j=1}^{6}w_iw_jS_iS_j\log D_{ij}$$

abhenrys per centimeter of bus length (36)

Substituting in equation 36 as indicated and collecting terms yields

$$L=-[4(S_1-2S_2)^2][S_1^2(\log D_{11}-\log D_{16})+S_2^2(2\log D_{22}+2\log D_{23}-2\log D_{24}-\log D_{25}-\log D_{34})-2S_1S_2(2\log D_{12}-\log D_{14}-\log D_{15})]$$

abhenrys per centimeter of bus length (37)

EXAMPLE 5

A three-phase line is comprised of three equilaterally spaced rectangular tubular conductors (Figure 5). To calculate the inductance associated with one conductor, say a:

Each rectangular tubular conductor can be considered as comprised of two component conductors: one of rectangular cross section wherein current exists in the same direction as in the tubular conductors, the other of smaller rectangular cross section having current in the opposite direction: Hence: $S_1=S_3=S_5$; $S_2=S_4=S_6$; $w_1=-w_2$; $w_3=-w_4=w_1\epsilon^{-120°}$; $w_5=-w_6=w_1\epsilon^{+120°}$. By equation 27 the associated inductance of the conductor a is

$$L_a=-(2/I_a^2)\sum_{i=1}^{2}\sum_{j=1}^{6}w_iw_jS_iS_j\log D_{ij}$$

abhenrys per centimeter of conductor length (38)

Noting that $w_1=I_a/(S_1-S_2)$, we obtain on substituting in equation 38 as indicated and collecting terms

$$L_a=-[2/(S_1-S_2)^2][S_1^2(\log D_{11}+\epsilon^{-j120°}\times\log D_{13}+\epsilon^{+j120°}\log D_{15})+S_2^2(\log D_{22}+\epsilon^{-j120°}\log D_{24}+\epsilon^{+j120°}\log D_{26})+2S_1S_2(\log D_{12}+\epsilon^{-j120°}\log D_{14}+\epsilon^{+j120°}\log D_{16})] \quad (39)$$

Corroboratively, if $S_2=S_4=S_6=0$, the conductors are solid, and equation 39 reduces to the well-known expression

$$L_a=(2/I_a)[I_a\log(1/D_{aa})+I_b\log(1/D_{ab})+I_c\log(1/D_{ac})]$$

abhenrys per centimeter of conductor length (40)

Equation 40 also follows from equation 39 for the case of tubular conductors which are so thin that the cross sections can be considered as but comprised of four line segments. A specific treatment of this particular type of conductor and several numerical examples are to be found in a paper by Dwight and Wang:[27] they consider but square cross sections.

Appendix

Explicit evaluation of the quadruple integral

$$\int_{D+R}^{D+R+r}\int_{P+S}^{P+S+s}\int_0^r\int_0^s\log[|Y-y|^2+|X-x|^2]^{1/2}dydxdYdX$$

varies according as: (*i*) both P and D are positive; (*ii*) one or both of P and D is zero or is negative. In the first case, no logarithmic singularities are encountered in the course of evaluating the integral; whence integration can be effected directly through use of known tabulated integrals. In the second case, however, logarithmic singularities are encountered. These singularities necessitate repeated use of limits; whence the evaluation is considerably complicated. Additionally, as these singularities occur in a manner that varies with the relative position of the two rectangular areas of Figure 1, the process of integration must be modified for each possible distinct relative location; that is, for each possible combination of P and D mentioned in case (*ii*). Each of these various integrations has been effected; the very lengthy, purely formal analysis incident to evaluation is contained in a complementary paper to be published elsewhere. These integrations, however, differ only in mathematical detail from that for case (*i*), which follows. As mentioned, this case ($P, D>0$) is the simplest to evaluate: even so, the easily reproduced, elementary analysis is so extensive and space filling that it is deemed advisable to present but the end result of successive integrations.

First

$$\int_0^s\log[(Y-y)^2+(X-x)^2]^{1/2}dy=\Big[-\frac{1}{2}(Y-y)\log[(Y-y)^2+(X-x)^2]-(X-x)\tan^{-1}(Y-y)/(X-x)+(Y-y)\Big]_{y=0}^{y=s} \quad (41)$$

From equation 41 we obtain

$$\int_0^r\int_0^s\log[(Y-y)^2+(X-x)^2]^{1/2}dydx=\Big[\Big[\frac{1}{2}(Y-y)(X-x)\log[(Y-y)^2+(X-x)^2]-\frac{1}{2}[(Y-y)^2-(X-x)^2]\times\tan^{-1}(Y-y)/(X-x)+\pi(Y-y)^2/2-(Y-y)(X-x)/2+(Y-y)x\Big]_{y=0}^{y=s}\Big]_{x=0}^{x=r} \quad (42)$$

From equation 42 we obtain

$$\int_{P+s}^{P+S+s}\int_0^r\int_0^s[\log(Y-y)^2+(X-x)^2]^{1/2}dydxdY=\Big[\Big[\Big[\frac{1}{4}(Y-y)^2(X-x)-\frac{1}{12}(X-x)^3\log[(Y-y)^2+(X-x)^2]-[1/6(Y-y)^3-1/2(Y-y)(X-x)^2]\tan^{-1}(Y-y)/(X-x)+\pi(Y-y)^3/6-5(Y-y)^2(X-x)/12+(Y-y)^2x/2\Big]_{y=0}^{y=s}\Big]_{x=0}^{x=r}\Big]_{y=P+s}^{y=P+S+s} \quad (43)$$

From equation 43 we obtain

$$\int_{D+r}^{D+R+r}\int_{P+s}^{P+S+s}\int_0^r\int_0^s\log[(Y-y)^2+(X-x)^2]^{1/2}dydxdYdX=\Big[\Big[\Big[\Big[-\frac{1}{48}\times[(Y-y)^4-6(Y-y)^2(X-x)^2+(X-x)^4]\log[(Y-y)^2+(X-x)^2]-\frac{1}{6}[(Y-y)^3(X-x)-(Y-y)(X-x)^3]\times\tan^{-1}(Y-y)/(X-x)-13(Y-y)^2\times(X-x)^2/48+\pi X(Y-y)^3/6+(Y-y)^2Xx/2\Big]_{y=0}^{y=s}\Big]_{x=0}^{x=r}\Big]_{Y=P+s}^{Y=P+S+s}\Big]_{X=D+r}^{X=D+R+r} \quad (44)$$

Finally, substituting the limits in the order indicated and collecting terms, we obtain from equation 44 that

$$\int_{D+r}^{D+R+r}\int_{P+s}^{P+S+s}\int_0^r\int_0^s\log[(Y-y)^2+(X-x)^2]^{1/2}dydxdYdX=-(25/12)-(1/24)\sum_{i=1}^{4}\sum_{j=1}^{4}(-1)^{i+j}K(A_i,B_j) \quad (45)$$

wherein A_i, B_j, and $K(A_i, B_j)$ designate the quantities defined in equations 4, 5, and 6.

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8. THEORY AND PRACTICE OF ABSOLUTE MEASUREMENTS IN ELECTRICITY AND MAGNETISM (book),

Steady-State and Transient-Stability Analysis of Series Capacitors in Long Transmission Lines

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IN long-distance large-block power transmission, where is reactance compensation applicable? The complete answer to this question involves the weighing of compensated line performance against its costs, tempered with engineering judgment. This paper presents the results of an analysis made to determine basic performance data of compensated lines under steady-state and transient conditions. The data presented therefore allow the question to be partly answered and, it is believed, to a sufficient degree to permit certain general conclusions to be drawn. The system designer is thus provided with the fundamentally necessary data permitting him to proceed with an economic analysis for his particular case, where the cost factors are available. It is felt that in this field lies the value of these data. The costs of the various factors for a particular case can be weighed with the performance obtained by the same factors. The best design must necessarily be one where per cent line compensation, number of parallel circuits, generator short-circuit ratio, and number of intermediate switching stations are all used to their optimum degree for a given reliability of service. The number of factors which may be varied obviously depends upon whether compensation is to be used for increasing the power limits of a system already in operation or to be used as a factor in the design of a projected system.

The urgency of the times and the requirements the war effort is placing and will place on the transmission systems, and projected, coupled with the necessity of careful use of critical materials, emphasizes the importance and timeliness of the subject. The compensated line concept is not new, but the basic performance data presented are, and they should prove useful in helping to determine the expected gains of reactance compensation in projected and existing systems. A more complete analysis can be made for a particular system when it is necessary to determine in detail the effect of various operating conditions and arrangements peculiar to the system under consideration.

General Conclusions

The data given are specifically performance data, showing what can be accomplished technically with compensated lines. A satisfactory excursion into the economic realms is outside the scope of this paper. The availability of certain materials during the present times—as well as during normal times—obviously will have its influence in the ultimate solution of a transmission problem.

1. The first and obvious conclusion is: Reactance compensation is an effective method for increasing the transient- and steady-state-stability limits of a transmission system.

2. Existing systems, composed of machines with average characteristics and lines as short as 150–175 miles—whose characteristics are fixed, because the machines are already installed—can materially increase their stability limits with acceptable margins by reactance compensation.

3. The relatively large gains available through the use of special generator characteristics, intermediate switching stations, high-speed switching and relaying indicate their fundamental usage in *projected systems* above 200 miles in length whether or not reactance compensation is used. The economic line-length threshold of reactance compensation (not determined in this paper) will obviously vary in projected systems as the relative costs of all factors change.

4. Reactance compensation appears to be an essential requirement for a-c transmission of economic blocks of power with lines above 300 miles in length.

5. Since reactance compensation provides an effective manner of increasing the stability limits above the original design values,

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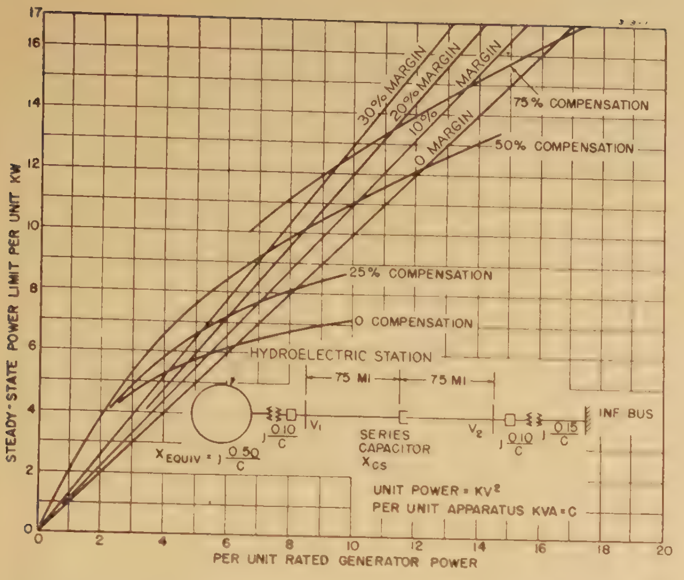


Figure 1. Steady-state stability limit of 150-mile line as a function of rating of hydroelectric generation at sending end

Transformer and low-voltage receiving-system capacitance increased in proportion to generator capacitance. Assumed generator power factor = 0.95. Held $V_1 = 1.0$
$V_2 = 0.95$

their possible use should tend to encourage a consideration of larger conductor sizes in order that the benefits from their use may be fully realized at the heavier system loadings.

6. The voltage rating of the series-capacitor equipment should be determined by—among other factors—the current during a stable-system transient oscillation, if maximum effectiveness is to be realized. This may mean a capacitor with higher than normal dielectric strength.

General Method of Analysis, Base Used, and System Studied

(*a*). The data presented in this paper, particularly those relating to transient stability, were obtained with the aid of an a-c network analyzer. The system was set up, with constants selected as described in the appendix and torque-angle curves obtained for the various conditions of fault on and off. Critical switching times were obtained by the application of the equal-area criterion.[1] Much of the steady-state work was done by well-known graphical methods.[2]

(*b*). It has been shown previously[3] that the loading of a transmission circuit varies with the square of the line voltage. For this reason it was considered convenient to make these studies on a kilovolt-ampere and kilowatt base which is given by the (kilovolts)^2 of the circuit under consideration Thus the data presented are entirely general and useful for any actual quantity of power and any value of voltage, except as the relation between these two quantities affects the per-unit power. The per-unit power transmitted on a (kilovolts)^2 base is given by the ratio

$$\frac{\text{kilowatts transmitted}}{(\text{transmission kilovolts})^2}$$

[For example, 150,000 kw on a (kilovolts)^2 base of (220)^2 gives a per-unit power of 3.1 and on a (161)^2 base gives a per-unit power of 5.0.]

(*c*). Based on a line surge impedance of 400 ohms, which is very nearly constant for all high-voltage lines, it has been shown[3] that the surge impedance or unity power-factor loading of such lines is 2.5 times the (kilovolts)^2; hence this value of per-unit circuit loading on a (kilovolts)^2 base is the unity power-factor loading. Since at this loading the line reactive losses are entirely supplied by the charging kilovolt-amperes of the line itself, it is generally considered a good operating loading. With reactance compensation, part of the line reactive losses are supplied by the series capacitors; hence the loading which will consume the line-charging kilovolt-amperes is increased.

(*d*). A typical hydroelectric system was selected as the basis for this study, since long lines in general involve this type of system, and it is this type that generally has the more severe stability problems. A sketch is included with each curve showing the system arrangement applying for the particular case studied. Refer to the appendix for reactances and constants chosen.

(*e*). The stability analysis in general involved the selection of a generator kilovolt-ampere rating of C times the (kilovolts)^2. For each such rating selected, the rating of other terminal apparatus was made to match also by dividing its reactance on rating by the factor C, as discussed in Appendix II. In the steady-state-stability studies, summarized in Figures 1 to 3 inclusive, various values of system rating C were selected and the power limit for each determined. For convenience, the diagonal per cent margin line was included which intersects the power-limit curve at the per cent margin indicated. For the transient-stability studies, Figures 9 to 16 inclusive, various system ratings of C were selected and for each the critical switching time plotted, assuming the sending-end generator fully loaded before the fault.

Discussion of Results

I. STEADY-STATE STABILITY, SINGLE-CIRCUIT LINE—FIGURE 5

(*a*) *Results With Average Generator Characteristics*

1. *250 Miles.* Zero compensation permits a per-unit generator rating of 2.9 to transmit full load with 20 per cent margin. This value is substantiated by present practices.[5] In the future heavier loadings are technically feasible, and they may be justified by reactance compensation; for instance, 50 per cent compensation allows a rating of 5.1 with 20 per cent margin.

2. *300 Miles.* A per-unit rating of 2.5, generally referred to as the surge-impedance loading of an uncompensated line or the loading where the line supplies its own I^2x losses can be handled at this line length with:

0 compensation with 10 per cent margin
25 per cent compensation with 25 per cent margin

Thus, for this line length and this type of system, it appears that some compensation would be needed for steady-state stability for transmission of economical magnitudes of power depending upon the required margin.

3. *400 Miles.* A per-unit generator rating of only 2 can transmit full load with

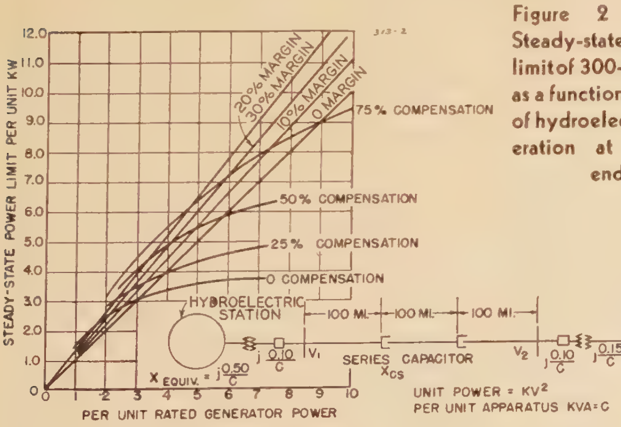


Figure 2 (left). Steady-state stability limit of 300-mile line as a function of rating of hydroelectric generation at sending end

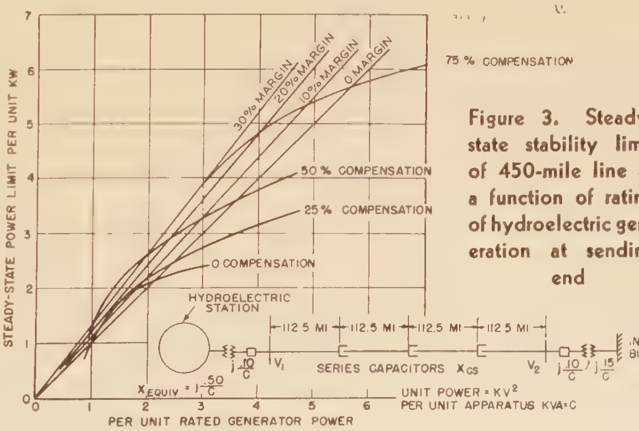


Figure 3. Steady-state stability limit of 450-mile line as a function of rating of hydroelectric generation at sending end

10 per cent margin a distance of 400 miles with no compensation. This indicates that series capacitors are undoubtedly required in lines of this length to transmit justifiable amounts of power.

(*b*) *Amount of Compensation.* There is an optimum value of compensation from a stability standpoint for a given line length. For a 300-mile line this value of compensation is about 75 per cent for the system constants chosen as shown in Figure 7. As the line length increases, this value also increases. Above this optimum value the over-all net impedance is predominantly resistance, so that voltage difference rather than phase angle tends to govern load flow. This is shown clearly in Figure 7 where the power which may be transmitted for compensation greater than optimum is larger for a line having five per cent voltage drop than for a line with flat regulation.

Table I

No. Switching Stations	Power Limits for Four Cycles Clearing—Two Lines (Per Cent Compensation)			
	0	25	50	75
0				
1				2.37
2			3.25	4.4
3		2.6	4.0	6.05
4	2.7			

(*c*) *Effect of Generator Short-Circuit Ratio.* See Figure 8. This factor was investigated for the 300-mile line operating at 20 per cent margin with the following results:

1. A compensation of 25 per cent will permit use of 1.2 short-circuit ratio in place of 2.0 with zero compensation, in transmitting output of a 2.5 per-unit generator.

2. A compensation of 50 per cent will permit use of short-circuit ratio of 1.3 in place of 2.5 with 25 per cent compensation in transmitting output of a 4.0 per-unit generator.

(*d*) *Summary, Steady-State Single-Circuit-Line Results.* It appears that, using a generator of average constants with a line length approaching 300 miles, some series compensation is required for steady-state stability to transmit more than that corresponding to the surge impedance loading of an uncompensated line, and so it follows that with loadings above this for line lengths below 300 miles, some compensation may be required, depending upon the generator characteristics. It should be noted that, while the data for steady-state stability are given for one circuit line, they apply for multiple circuit lines with all lines in on a per-circuit basis; for example, two such units studied may be operated in parallel with a steady-state limit of twice that given.

II. TRANSIENT STABILITY, SINGLE-CIRCUIT LINE, STUB-FEEDER FAULTS

(*a*). *Results With Average Generator Characteristics.* The stub-feeder type of fault was considered the most severe worth studying, since a fault on the main line would obviously result in an unavoidable loss which is a well recognized limitation of single-circuit lines. In evaluating these data, it is felt that a total clearing time of six cycles is reasonable, as this type of fault would occur in the low-voltage system at the receiver end. While these data were obtained, using a single-circuit line, they apply on a *per-circuit* basis to multicircuit lines.

Table II

	Read From Figure	At 230 Kv	At 287 Kv
Per-unit kilowatts transmitted		4.72	3.03
Per-unit kilowatts per circuit		2.36	1.52
Steady-state power limit per circuit	2		
0 compensation		2.7 (14.5 per cent margin)	2.1 (38 per cent margin)
25 per cent compensation		3.1 (31 per cent margin)	2.1*
50 per cent compensation		3.4 (46 per cent margin)	2.2*
75 per cent compensation		3.7 (57 per cent margin)	2.4*
Fault-clearing time—cycles			
Two switching stations	15		
25 per cent compensation			2.3
50 per cent compensation		0.8	4.3
75 per cent compensation		3.55	5.2
Three switching stations	16		
25 per cent compensation		0 (nearly)	3.7
50 per cent compensation		2.65	5.0
75 per cent compensation		5.3	6.0

*Estimated

1. *150 Miles.* It is found in the study that the full output of a per-unit generator rating of 2.8 can be transmitted with zero compensation, while a rating of 4.5 can be transmitted with 25 per cent compensation. If line loading above approximately 3 is desirable, then series capacitors will reasonably permit such operation in this area.

2. *300 Miles—Figure 10.* It is interesting to note for this case that 50 per cent compensation is required to transmit any reasonable power. For instance, with six cycles clearing, the full output of a maximum generator rating of 3 may be carried through a stub feeder fault with 50 per cent compensation. This loading is not particularly heavy, and so more compensation in a particular instance may be justified. For example, it is technically possible to transmit (eight kilovolts)^2 and 75 per cent compensation, but such loading will be tempered of course by economics.

3. *450 Miles—Figure 11.* With a switching time of six cycles, 66 per cent compensation is required to transmit full load of a per-unit generator rating of 2.5. This indicates that compensation is needed to justify a line of this length. Further, the allowable compensation falls within a rather narrow range, something between 65 per cent and 80 per cent (Figures 7 and 11).

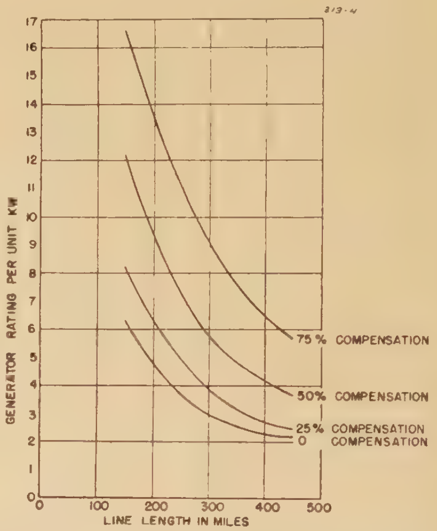


Figure 4. Data from Figures 1, 2, and 3 showing power limit in terms of generator rating with zero margin for various line lengths

Unit power=(kilovolts)^2

The stub-feeder faults as shown in Figure 11 indicate that the transient limit for small switching time is higher than the steady-state limit. These two boundaries define the results obtaining during the first swing and after an infinite number of swings, respectively. Because of the labor involved in determining completely this transition, only the two boundaries were given, so that the upper portion of these curves should not be taken too literally, but only as an indication of magnitude.

(*b*). *Interpretation for Other Conditions.* It should be noted that all of these data permit cross-plotting for interpolating and determining switching times for values of line length and per cent compensation different than those chosen. Further, the switching times for a given set of conditions, but utilizing machines with different inertias, may be deter-

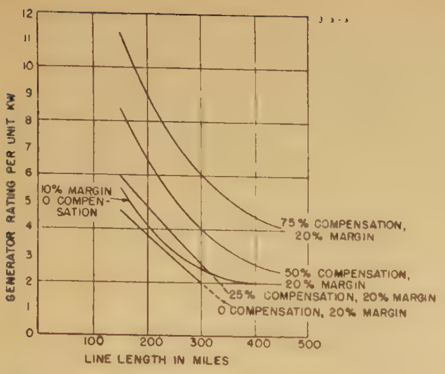


Figure 5. Data from Figures 1, 2, and 3 showing power limit in terms of generator rating with 10 per cent and 20 per cent margin for various line lengths

Unit power = (kilovolts)²

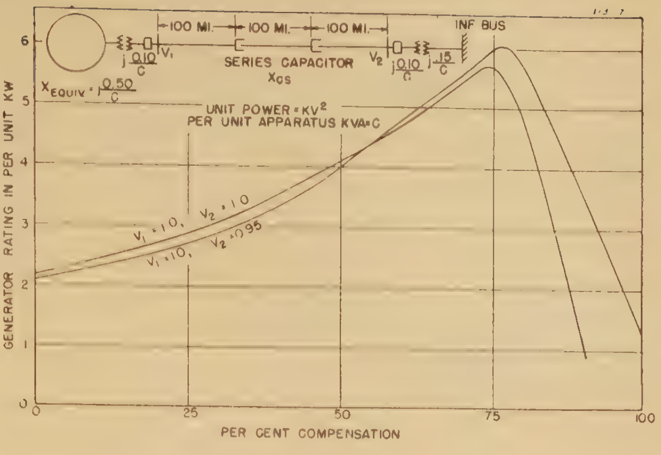


Figure 7. Effect of line voltage level on steady-state limit

Power limit in terms of generator rating with 20 per cent margin, for 300-mile line

mined by increasing the time by the factor $\sqrt{H/3}$, H being the new constant and 3 the value used in obtaining the data.

III. STEADY-STATE STABILITY, TWO PARALLEL LINES—FIGURE 12

For two-line circuits the worst fault through which the system may ride is a three-phase high-voltage fault at the sending end, resulting in loss of a line section. In view of this it is desirable to investigate the steady-state stability of the system with one line section out.

The multiplicity of the variables involved limited the scope of the investigation in general to a line length of 300 miles. This value was chosen since lengths in this area are being considered for future power transmission, and this, as well as previous studies, indicates that such lengths fall within a region where series capacitors should be given consideration.

The data summarized in Figure 12 show clearly how switching stations may be traded for compensation. It is interesting to note that 25 per cent compensation in the center of the line utilizing one switching station is equivalent to a large number of switching stations for steady-state stability with average generator constants. However, it must be remembered that the number of switching stations is dictated primarily by the transient-stability requirements.

IV. TRANSIENT STABILITY, TWO PARALLEL LINES

In the analyses, data were obtained enabling the comparative effects of switching stations, per cent compensation, and clearing times to be studied. In evaluating the data, a total clearing time of four cycles will be used.

(*a*) *Results With Average Generator Characteristics*

1. *150 Miles—Figure 13.* Zero compensation allows a per-unit generator rating of 2.7 with no intermediate switching stations. As can be seen from Figure 13, probably more than the economic value of power can be transmitted with no compensation by the proper choice of switching stations. Economics obviously would determine whether switching stations, special generator characteristics, or compensation should be used to accomplish a given result.

2. *300 Miles.* The data given in Figures 14 through 17 for two parallel lines with 0–4 switching stations for various degrees of compensation are summarized in Table I for a clearing time of four cycles. Here the comparative effects of compensation and switching stations are readily seen. For example, 25 per cent compensation with three switching stations permits nearly the same per-unit generator rating as four switching stations with zero compensation.

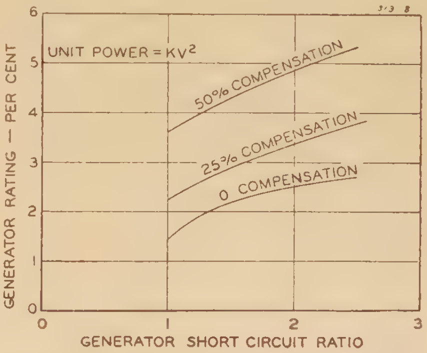


Figure 8. Effect of generator short-circuit ratio on steady-state limit

Power limit in terms of generator rating with 20 per cent margin, for 300-mile line. Assumed $x_{d(\text{equiv})} = 0.7/\text{short-circuit ratio}$

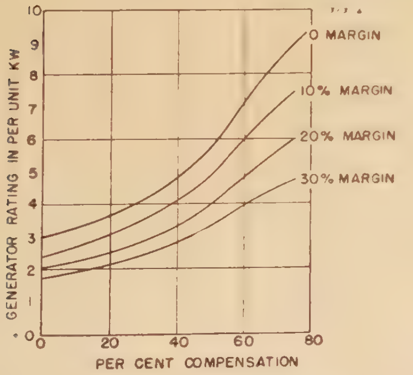


Figure 6. Data from Figure 2 showing effect of margin on required compensation, for a 300-mile line

Unit power = (kilovolts)². Steady-state power limit = generator rating (1 + margin)

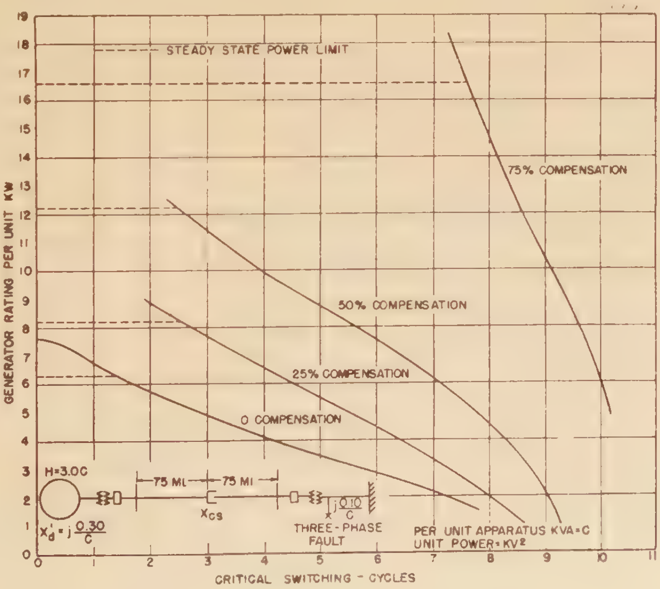


Figure 9. Switching-time curve for three-phase stub-feeder fault at low-voltage receiving end, 150-mile single-circuit line

Hydroelectric generating station carrying full rated load before fault

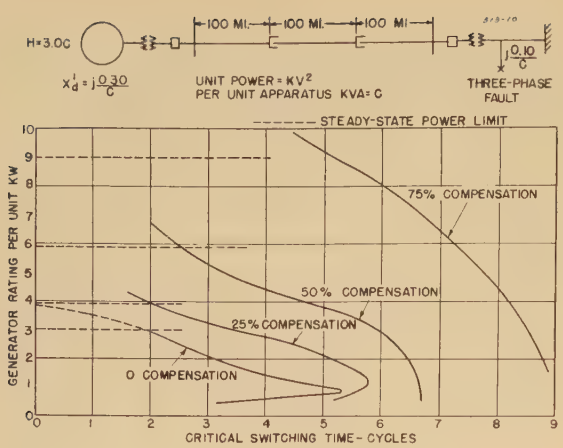


Figure 10. Switching-time curve for three-phase stub-feeder fault at low-voltage receiving end, 300-mile single-circuit line

Hydroelectric generating station carrying full rated load before fault

Similarly, two switching stations with 75 per cent compensation gives more margin than three switching stations with 50 per cent compensation.

Stability cannot be maintained for four cycle clearing time for the full output of any generator rating with the combination of conditions identified by the vacancies in Table I.

These loadings are well under the unity power-factor loadings, which are usually considered good average economic values. Hence, it appears fundamental that special generator characteristics such as high inertias and low transient reactances should be used in long lines whether compensation is used or not.

(*b*). *Interpretation of Results for Different Machine Characteristics*

1. *Effect of Generator Transient Reactance—Figure 18.* The effects on clearing time of a change in the machine reactance for several combinations of compensation and switching stations for a 300-mile line were studied. The results given in Figure 18 indicate that with a clearing time of four cycles, the full output of a per-unit generator rating of 2.5 can be transmitted with a 20 per cent transient reactance, two switching stations, and no compensation, as compared with a 30 per cent transient reactance, one switching station, and 50 per cent compensation.

2. *Effect of Generator Inertia.* Switching times given by these data may again be readily modified to apply to machines having inertia constants different from that used in the study, since they vary as the $\sqrt{H}$.

Refer to Figure 15 to see the effect of changing both H and x_d' in a particular instance.

V. PROTECTIVE EQUIPMENT AND CAPACITOR VOLTAGE RATINGS

In addition to normal load current through series capacitors, short-circuit currents and system swing currents after fault clearing must be considered.

Protective equipment will be required in general for short-circuiting the capacitors during the fault period. After the fault is removed the magnitude of the swing current may be such that it might be necessary to short-circuit the capacitors. Reference to Figure 19, which shows times normal swing currents which were measured during the single circuit—stub-feeder fault study, indicates that for a 150-mile line having 75 per cent compensation and a per-unit kilovolt-ampere loading (C) of 4.0 (which is probably less than could be justified for this type of circuit), the swing current reaches 2.7 times normal.

Referring to Figure 19, the maximum swing current is given by

$$I_S' = \frac{\bar{E}_1 - \bar{E}_2}{\dfrac{x_d' + x_t + x_t + x_S}{C} + x_l} = \frac{E_0}{\dfrac{x_a}{C} + x_l}$$

where

x_a = total apparatus reactance on rating
x_l = net line reactance
C = per-unit apparatus kilovolt-amperes

Since the per-unit normal capacitor current is C, the times normal swing current is

$$I_S = \frac{I_S'}{C} = \frac{E_0}{x_a + x_l C}$$

This shows that, as the kilovolt-amperes transmitted (C) increases, the maximum swing current decreases for a given net line reactance. Also, as the line reactance decreases holding C constant, the swing current increases, as would be expected.

For the system selected, the maximum swing current will not exceed 2.7 times normal because

(*a*). This value is obtained with a low value of C for the shortest line with the most compensation resulting in the lowest x_l.

(*b*). For greater values of C and x_l, the swing current will be less as shown above and by the curves, Figure 19.

These same per-unit values hold for multi-circuit lines with all circuits in. For the parallel line case with one line section

Figure 11. Switching-time curve for three-phase stub-feeder fault at low-voltage receiving end, 450-mile single-circuit line

Hydroelectric generating station carrying full rated load before fault

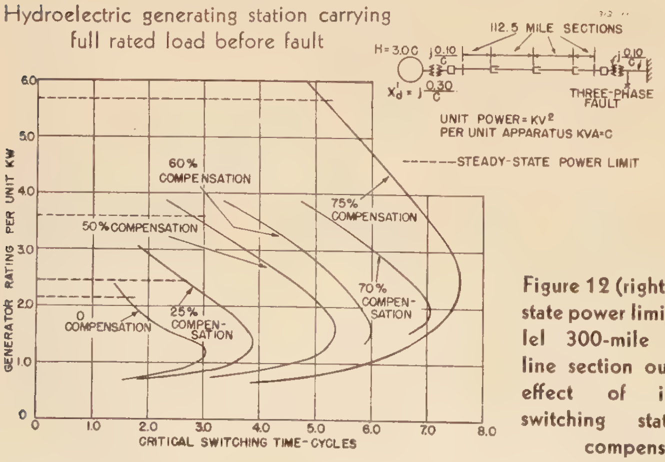
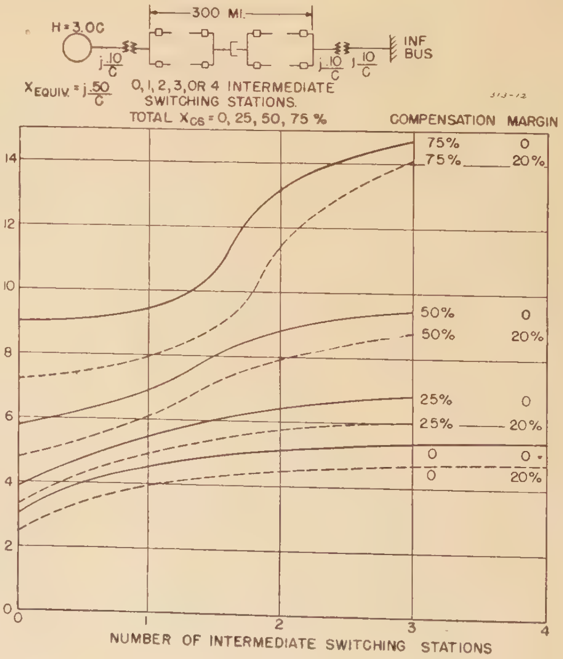


Figure 12 (right). Steady-state power limit, two parallel 300-mile lines, one line section out, showing effect of intermediate switching stations and compensation



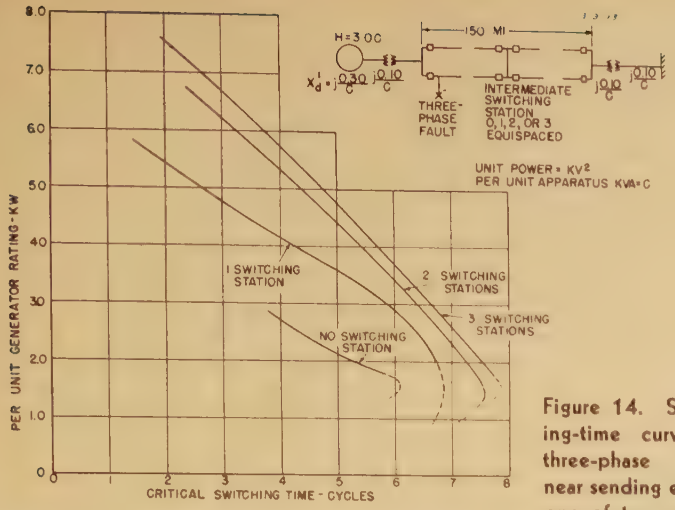


Figure 13. Switching-time curve for three-phase fault near sending end of one of two parallel 150-mile lines, for 0, 1, 2, and 3 intermediate switching stations

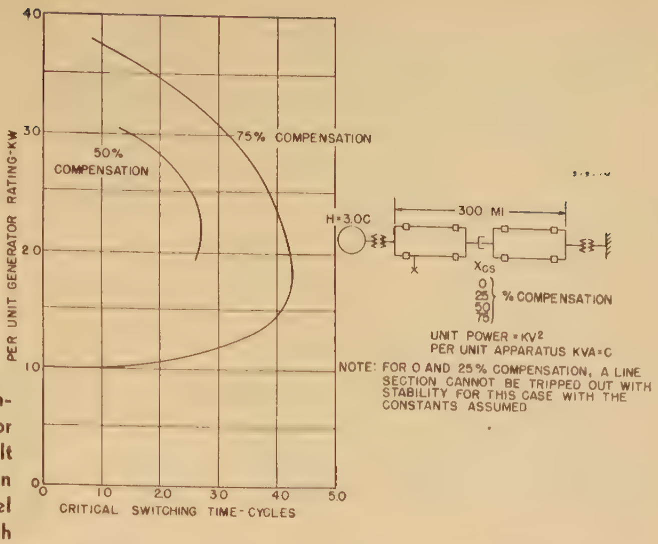
Fault results in tripping out of faulted line section. Hydroelectric generating station carrying full rated load before fault

tripped out as a result of a fault clearing, the swing currents will be less than shown in Figure 19, because x_l is increased by virtue of the removed line section. The capacitor unit voltage rating should thus be determined by these current swings if maximum effectiveness is to be realized.

For certain fault locations the currents may be at times of such magnitude as to require the action of protective equipment. After the fault is removed, the capacitors should be placed back in service immediately, if maximum effect in maintaining stability is to be realized.

Figure 14. Switching-time curve for three-phase fault near sending end on one of two parallel 300-mile lines, with one intermediate switching station

Fault results in the tripping out of the faulted line section. Hydroelectric generating station carrying full rated load before fault



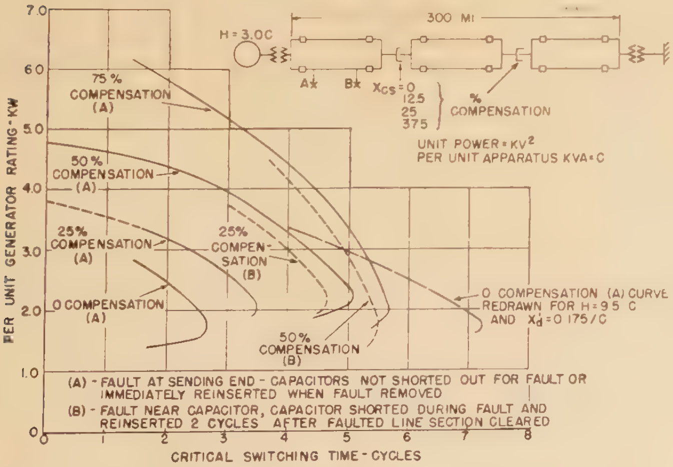
The effect of short-circuiting the capacitors instantaneously and then restoring them two cycles after the faulted line section is switched out is shown in Figure 15. This calculation involved the use of the equal area criterion as previously outlined.[4] The curves indicate that on this basis the most severe fault requiring the switching out of the capacitor is a little more severe than a three-phase fault on the sending end where the capacitors can be left in, or by-passed and immediately reinserted after the fault is removed with the same effect. It is thus shown that the effect of a two-cycle time delay in reinserting the capacitors is of such low magnitude that it could be neglected for the general study.

Figure 15. Switching-time curve for three-phase fault near sending end on one of two parallel 300-mile lines, with two intermediate switching stations

Fault results in the tripping out of the faulted line section. Hydroelectric generating station carrying full rated load before fault

A—Fault at sending end—capacitors not short-circuited out for fault or immediately reinserted when fault removed

B—Fault near capacitor—capacitor short-circuited during fault and reinserted two cycles after faulted line section cleared



Machine Hunting and Abnormal Voltages

It is well known that hydroelectric generators not equipped with amortisseur windings connected to long lines experience hunting during light-load periods. It is also known that series capacitors magnify and encourage such hunting. A good analysis and bibliography of this phenomenon is given in reference 6. In this analysis it is shown that machines equipped with suitable amortisseur windings will not experience hunting when transmitting power over lines up to 300 miles (the extent of the study) in length and fully compensated. In view of these findings, and since a maximum of 75 per cent compensation is useful for stability reasons, it is felt that properly designed damper winding will prevent this difficulty.

Reference 7 of the paper presents an analysis of possible abnormal voltages

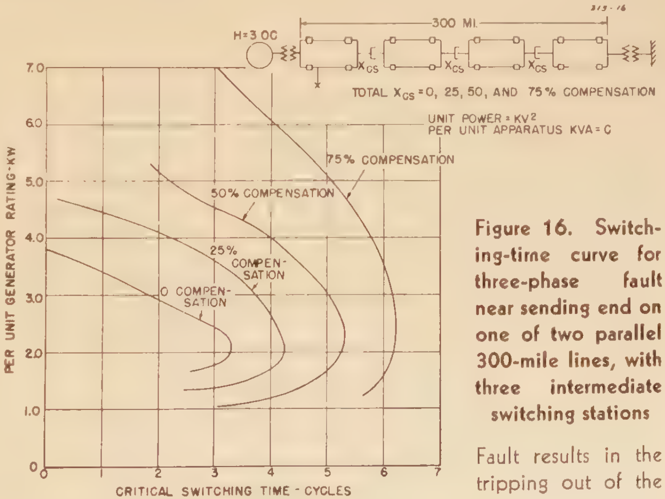


Figure 16. Switching-time curve for three-phase fault near sending end on one of two parallel 300-mile lines, with three intermediate switching stations

Fault results in the tripping out of the faulted line section. Hydroelectric generating station carrying full rated load before fault

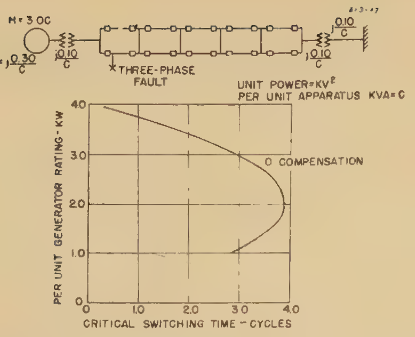


Figure 17. Switching-time curve for three-phase fault near sending end on one of two parallel 300-mile lines, with four intermediate switching stations

Fault results in the tripping out of the faulted line section. Hydroelectric generating station carrying full rated load, before fault

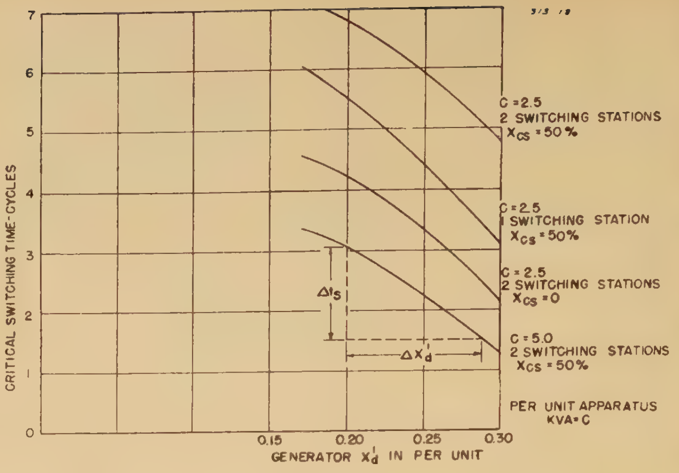


Figure 18. Critical switching time as affected by transient reactance

300-mile line. Machine at full rated load before fault

occurring in long lines in general, and in lines having series capacitors in particular when an unloaded or lightly loaded transformer is left connected on the receiving end. These overvoltages become possibilities when low-side switching is used. Since high-side switching is generally necessary for stability, this should not present a serious problem. Attention is directed to this phenomenon so that it will receive just consideration in a particular instance.

Illustration of the Use of the Curves

Assume that it is desired to transmit the output of a 250,000-kw hydroelectric station a distance of 300 miles, over a two-circuit line, into a typical receiving system such as used in this study.

Consider two possible transmission voltages, 230 kv and 287 kv. The generator kilowatts are therefore

$$\text{At 230 kv } \frac{250{,}000}{(230)^2} = 4.72 \text{ per unit}$$

$$\text{At 287 kv } \frac{250{,}000}{(287)^2} = 3.03 \text{ per unit}$$

To determine the steady-state limit for such a system with all lines in, refer to Figure 2. Steady-state data given by Figures 1 through 8 are on a per circuit basis, hence the above per-unit kilowatts must be divided by two for the two-circuit line under consideration.

Table II indicates the information of interest and the curves on which the values are found.

The fault-clearing times given in Table II are of course for a three-phase fault near the sending end of one of the high-voltage lines, and are based either on the series capacitor remaining in the circuit or on its immediate reinsertion after the fault is removed. The *effect of increasing the generator inertia may be determined by multiplying these times by $\sqrt{\text{per-unit}}$ generator $H/3.0$.* An estimate of the additional clearing time obtainable by the use of a lower generator transient reactance may be obtained from Figure 18, taking the Δt_s for a given $\Delta x_d'$, or conversely, finding the $\Delta x_d'$ required for a needed Δt_s.

It is seen that if four-cycle total clearing time is used, and the lines are designed for 230 kv, transient stability may be maintained, for example with:

(*a*). Two switching stations and 75 per cent compensation, and a per-unit generator H of 3.8 rather than 3.0, (or an x_d' of 0.28 instead of 0.30).

(*b*). Three switching stations, 50 per cent compensation, per unit $H = 6.85$ (or an x_d' of 0.23).

(*c*). Three switching stations, 75 per cent compensation.

If the lines are 287 kv, transient stability through a three-phase high-voltage sending-end fault may be maintained with

(*a*). Two switching stations, 25 per cent compensation, per unit $H = 9.1$ (or an x_d' of 0.20).

(*b*). Two switching stations and a little less than 50 per cent compensation.

The most economic combination of transmission voltage, reactance compensation, generator inertia, and short-circuit ratio may be obtained with cost data and an analysis along these lines.

Interpretation of Data for Generators Having Different Characteristics

The generator characteristics used were considered representative of the average hydroelectric installation. Projected systems involving long lines will undoubtedly have larger short-circuit ratios, lower transient reactances, and larger inertia constants than the average; hence a word on the interpretation of results for such cases will be given.

Consider a 300-mile two-circuit uncompensated line with two intermediate switching stations, with machine constants as follows:

x_d'	H constant
0.175	9.5—Boulder Dam Units[4]
0.30	3 —Used in this study

These are the constants of the 82,500-kw generators at Boulder Dam, and they represent a large variation from the average taken for this study.

(*a*) Correction for Inertia (H) Constant

The critical switching time for a given set of system conditions varies as the $\sqrt{H}$, and so for an H of 9.3, the critical switching time should be increased by $\sqrt{9.3/3} = 1.78$. Referring to Figure 15, for the case in question, the switching times of the curve for zero compensation should all be multiplied by 1.78 for a given generator rating.

(*b*). Correction for Transient (x_d') Reactance

From Figure 18 it is seen that 2.5 cycles is a conservative correction to make in the switching time when decreasing x_d' from 0.30 to 0.175. Making this correction to a "zero compensation" curve as located by H constant correction in the preceding paragraph, the dotted curve in Figure 18 is obtained. This new curve shows that the full output of a 2.4 per-unit rated generator having the new characteristics can be carried through a three-phase fault at the sending end of a 300-mile line with a total clearing time of four cycles. At 287.5 kv this corresponds to 280,000 kw, essentially agree-

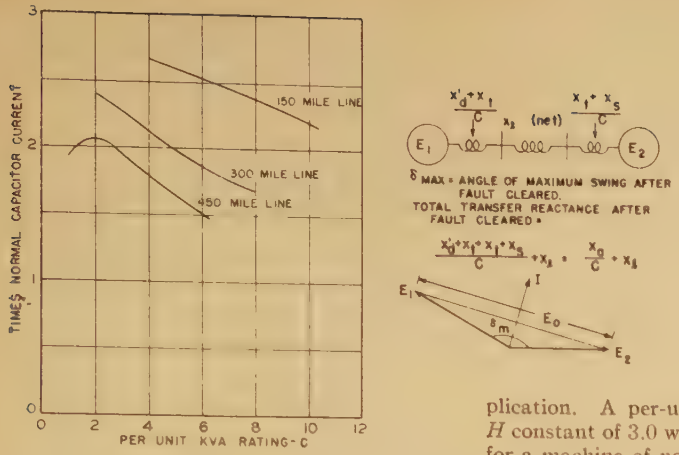


Figure 19. Maximum swing current with stability after clearing stub-feeder fault

Line 75 per cent compensated

ing with published calculated performance data for the 287.5-kv Boulder Dam–Los Angeles lines.[8]

Appendix I. Description of System Studied

A hydroelectric station, whose per-unit-kilovolt-ampere rating, called a factor C (see Appendix II), was varied, supplies power to an infinite bus through:

1. A step-up transformer bank of 0.10 per-unit reactance.
2. A transmission circuit of typical 60-cycle constants.
3. A step-down transformer bank of 0.10 per-unit reactance.
4. A receiving system having a steady-state per-unit reactance of 0.15 and a transient reactance of 0.10.

Constants selected for the generator in per unit on its rating were

$$x_{\text{equiv}}=0.50=\frac{0.70}{\text{short-circuit ratio}}$$

$$x_d'=0.30$$

This corresponds to a short-circuit ratio of 1.4, which is representative of the average machine. The effects of both lower and higher short-circuit ratios were investigated. A generator power-factor rating of 0.95 was used as a nominal value for this type of application. A per-unit generator inertia or H constant of 3.0 was used, which is typical for a machine of normal short-circuit ratio. Since the critical switching times vary directly with $\sqrt{H}$, the effect of other values may be readily evaluated. The line constants selected as typical and nearly constant regardless of voltage for high-voltage 60-cycle lines, were

$r+jx=0.12+j0.80$ ohms per mile
$g+jb=0+j5.2\times10^{-6}$ mhos per mile

The relationship between actual ohms and per-unit ohms on a given kilovolt-ampere base for a particular kilovolt is:

$$\text{per-unit ohms}=\frac{\text{actual ohms}\times\text{kilovolt-ampere base}}{(\text{kilovolts})^2\ 1{,}000}$$

Hence on the kilovolt-ampere base $=$ (kilovolts)^2, used throughout this study

$$\text{per-unit ohms}=\frac{\text{actual ohms}}{1{,}000}$$

and therefore the line constants on the base selected varied only with line lengths. This was another convenience resulting from the use of this base.

Appendix II. A Note on the Factor C

A generator having a per-unit kilovolt-ampere rating of C as used in this study has a kilowatt rating of 0.95 C (kilovolts)^2, because a 0.95 power-factor machine was assumed throughout. For an equivalent reactance x_{equiv} of 0.50 on the kilovolt-ampere rating of C (kilovolts)^2 the reactance on the (kilovolts)^2 base is $0.50/C$. This follows from the relationship that

$$x_{\text{equiv}}\ (\text{on given base})=x_{\text{equiv}}\ (\text{on rating})\times\frac{\text{given base}}{\text{rating}}$$

Hence

$$x_{\text{equiv}}\ (\text{kv})^2\ \text{base}=x_{\text{equiv}}\ (\text{on rating})\ \frac{\text{kv}^2}{C(\text{kv})^2}=\frac{x_{\text{equiv}}\ (\text{on rating})}{C}$$

The same relation holds for the sending end and receiving end transformer and receiving system reactances, as indicated on the sketch accompanying each figure.

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A Useful Equivalent Circuit for a Five-Winding Transformer

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Synopsis: It has been indicated that there is a growing interest in the possibilities of multiwinding transformers with five windings. The lack of a suitable equivalent circuit for this type of transformer has retarded some of these engineering analyses. This paper presents a practical equivalent circuit, together with a development of the necessary formulas. Numerous mathematical manipulations have been omitted in the interest of brevity. Several of these manipulations are the key to the simplicity of the final working formulas. Examples show how the various equivalent-circuit parameters are evaluated. Factors affecting the voltage regulation of a five-winding transformer are brought out with the aid of an example in which two sources of supply are present. An objective of the analysis has been to keep the equivalent circuit suitable for the calculating board. Several precautions in this connection are discussed.

THE demand for electric power in greater quantities and with optimum continuity has increased the transmission engineers' interest in interconnected circuits and the multicircuit transformers associated therewith. Considerable has been published on three- and four-winding transformers.[1] This article enlarges the field to include a much more complicated unit, the five-winding transformer.

It should be emphasized that multicircuit transformers present many problems which cannot be solved by two-winding methods. These complications relate to leakage-impedance phenomena associated with the many fluxes illustrated in Figure 1. Among the more important characteristics affected by these phenomena are

(*a*). Voltage regulation.

(*b*). Load division between circuits.

(*c*). Parallel operation with other transformers.

(*d*). Efficiency.

(*e*). Short-circuit characteristics.

The interlinkage of the various leakage fluxes due to load currents in the different circuits affect the various voltages in complicated and sometimes unexpected ways. It is therefore necessary to have a clear conception of the leakage impedance relationships; otherwise the manipulation of the parameters may prove perplexing and unduly complicated.

Theory

The general theory of a five-winding transformer is no different from that with a lesser number of windings. Such a transformer consists of five separate windings that are mutually coupled by a single magnetic core, illustrated best as in Figure 1. This transformer can be analyzed in the conventional manner common to multicircuit transformers of a lesser number of windings, wherein the basic differential equations of voltage can be expressed as follows:

$$e_1=r_1i_1+L_1\frac{di_1}{dt}+M_{12}\frac{di_2}{dt}+M_{13}\frac{di_3}{dt}+$$
$$M_{14}\frac{di_4}{dt}+M_{15}\frac{di_5}{dt}$$

$$e_2=r_2i_2+L_2\frac{di_2}{dt}+M_{21}\frac{di_1}{dt}+M_{23}\frac{di_3}{dt}+$$
$$M_{24}\frac{di_4}{dt}+M_{25}\frac{di_5}{dt}$$

$$e_3=r_3i_3+L_3\frac{di_3}{dt}+M_{31}\frac{di_1}{dt}+M_{32}\frac{di_2}{dt}+$$
$$M_{31}\frac{di_4}{dt}+M_{35}\frac{di_5}{dt}$$

$$e_4=r_4i_4+L_4\frac{di_4}{dt}+M_{41}\frac{di_1}{dt}+M_{42}\frac{di_2}{dt}+$$
$$M_{43}\frac{di_3}{dt}+M_{45}\frac{di_5}{dt}$$

$$e_5=r_5i_5+L_5\frac{di_5}{dt}+M_{51}\frac{di_1}{dt}+M_{52}\frac{di_2}{dt}+$$
$$M_{53}\frac{di_3}{dt}+M_{54}\frac{di_4}{dt}$$

where the subscripts refer to the windings involved, and

e = applied voltage
r = winding resistance
i = winding current
L = coefficient of self-inductance
M = coefficient of mutual inductance

A rigorous mathematical treatment of the foregoing fundamental equations will lead to terms suitable for an equivalent circuit.

A true equivalent circuit for a transformer is one which cannot be distinguished from the actual transformer by any observations that can be made at its terminals. It is the *terminal* characteristics of a transformer and not the characteristics of some particular coil within the transformer that are of interest.

As in most practical transformer equivalent circuits, magnetizing current has been neglected. This results in an equivalent circuit of sufficient accuracy for most all power-circuit problems. The circuit contains components which represent winding resistance, self-inductance, and load or mutual impedance. Any winding can be used as the supply winding; in fact, more than one winding may be connected to a source of energy.

Properties of the Equivalent Circuit

It is a physical and mathematical necessity that a completely equivalent network have as many independent elements (or degrees of freedom) as the original system. The properties of a circuit for the problem at hand are:

1. The circuit must have five points of entry corresponding to the five windings of the transformer.

2. The circuit must have ten independent elements in order that the ten independent measurable impedances of the transformer may be simulated.

3. The circuit should be one that avoids excessive calculation. The impedances should be simple functions of the measurable transformer impedances obtained in commercial transformer production.

4. The circuit should be free from negative-impedance elements. Although this characteristic does not void the solution of any problem, it does complicate studies on a calculating board, as negative resistance cannot be easily provided. Moreover, a circuit with no negative reactance is desirable since it may be used on both the a-c and d-c calculating boards.

Upon designating the five windings of Figure 2, with the numbers 1, 2, 3, 4, and 5, the ten independent impedances which can be measured at the terminals of the transformer are:

$$\begin{array}{cccc} Z_{12} & Z_{13} & Z_{14} & Z_{15} \\ & Z_{23} & Z_{24} & Z_{25} \\ & & Z_{34} & Z_{35} \\ & & & Z_{45} \end{array}$$

Each of these impedances is measured by applying sufficient voltage to one of the windings, whose number appears in the subscript, to circulate a current in a short

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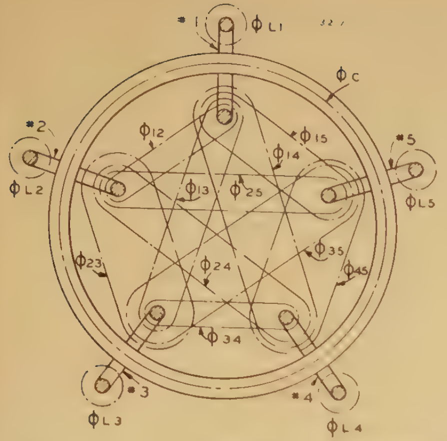

Figure 1. Flux linkages of a five-winding transformer

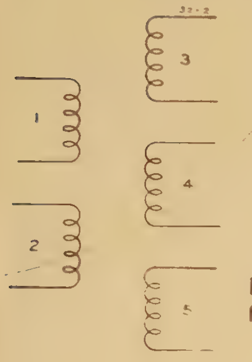

Figure 2. A simple five-winding transformer

circuit on the other winding, whose number appears in the subscript, its magnitude adjusted to the kilovolt-ampere base chosen. All other windings remain open-circuited.

The equivalent circuit used in this derivation of general formulas is that shown in Figure 3. Throughout the derivation of these general formulas, the loads indicated will not be considered.

Derivation of Formulas

Quantitative evaluation of the parameters for the five-winding transformer shown in Figure 3 can be obtained from these fundamental equalities:

$$Z_{12}=Z_1+Z_2+\cfrac{1}{\cfrac{1}{Z_6}+\cfrac{1}{Z_7+Z_8+Z_9+Z_{10}}} \qquad (1)$$

$$Z_{13}=Z_1+Z_3+\cfrac{1}{\cfrac{1}{Z_6+Z_7}+\cfrac{1}{Z_8+Z_9+Z_{10}}} \qquad (2)$$

$$Z_{14}=Z_1+Z_4+\cfrac{1}{\cfrac{1}{Z_6+Z_7+Z_8}+\cfrac{1}{Z_9+Z_{10}}} \qquad (3)$$

$$Z_{15}=Z_1+Z_5+\cfrac{1}{\cfrac{1}{Z_6+Z_7+Z_8+Z_9}+\cfrac{1}{Z_{10}}} \qquad (4)$$

$$Z_{23}=Z_2+Z_3+\cfrac{1}{\cfrac{1}{Z_7}+\cfrac{1}{Z_6+Z_8+Z_9+Z_{10}}} \qquad (5)$$

$$Z_{24}=Z_2+Z_4+\cfrac{1}{\cfrac{1}{Z_7+Z_8}+\cfrac{1}{Z_6+Z_9+Z_{10}}} \qquad (6)$$

$$Z_{25}=Z_2+Z_5+\cfrac{1}{\cfrac{1}{Z_7+Z_8+Z_9}+\cfrac{1}{Z_6+Z_{10}}} \qquad (7)$$

$$Z_{34}=Z_3+Z_4+\cfrac{1}{\cfrac{1}{Z_8}+\cfrac{1}{Z_6+Z_7+Z_9+Z_{10}}} \qquad (8)$$

$$Z_{35}=Z_3+Z_5+\cfrac{1}{\cfrac{1}{Z_8+Z_9}+\cfrac{1}{Z_6+Z_7+Z_{10}}} \qquad (9)$$

$$Z_{45}=Z_4+Z_5+\cfrac{1}{\cfrac{1}{Z_9}+\cfrac{1}{Z_6+Z_7+Z_8+Z_{10}}} \qquad (10)$$

These equations can be simplified by successive steps and solved simultaneously resulting in the following ten expressions:

$$Z_6Z_7=(K-2Z_2)\frac{Z_t}{2} \qquad (11)$$

$$Z_6Z_8=L\frac{Z_t}{2} \qquad (12)$$

$$Z_6Z_9=M\frac{Z_t}{2} \qquad (13)$$

$$Z_6Z_{10}=(N-2Z_1)\frac{Z_t}{2} \qquad (14)$$

$$Z_7Z_8=(O-2Z_3)\frac{Z_t}{2} \qquad (15)$$

$$Z_7Z_9=P\frac{Z_t}{2} \qquad (16)$$

$$Z_7Z_{10}=Q\frac{Z_t}{2} \qquad (17)$$

$$Z_8Z_9=(R-2Z_4)\frac{Z_t}{2} \qquad (18)$$

$$Z_8Z_{10}=S\frac{Z_t}{2} \qquad (19)$$

$$Z_9Z_{10}=(T-2Z_5)\frac{Z_t}{2} \qquad (20)$$

in which

$$K=Z_{23}+Z_{12}-Z_{13} \qquad (21)$$

$$L=Z_{13}+Z_{24}-Z_{14}-Z_{23} \qquad (22)$$

$$M=Z_{14}+Z_{25}-Z_1-Z_{24} \qquad (23)$$

$$N=Z_{12}+Z_{15}-Z_{25} \qquad (24)$$

$$O=Z_{34}+Z_{23}-Z_{24} \qquad (25)$$

$$P=Z_{24}+Z_{35}-Z_{34}-Z_{25} \qquad (26)$$

$$Q=Z_{13}+Z_{25}-Z_{12}-Z_{35} \qquad (27)$$

$$R=Z_{45}+Z_{34}-Z_{35} \qquad (28)$$

$$S=Z_{14}+Z_{35}-Z_{13}-Z_{45} \qquad (29)$$

$$T=Z_{45}+Z_{15}-Z_{14} \qquad (30)$$

and

$$Z_t=Z_6+Z_7+Z_8+Z_9+Z_{10} \qquad (31)$$

The links of the pentagonal mesh are evaluated by using the following algebraic relations in the second-degree equations 12, 13, 16, 17, and 19:

$$Z_7=aZ_6$$
$$Z_8=bZ_6$$
$$Z_9=cZ_6$$
$$Z_{10}=dZ_6$$

Equating various of the resulting expressions to the common quantity Z_6, these coefficients are obtained:

$$a=\frac{P}{M}$$

$$b=\frac{SP}{QM}$$

$$c=\frac{SP}{LQ}$$

$$d=\frac{S}{L}$$

Equations 11 to 20 inclusive are solved for various of the network parameters resulting in

$$Z_1=\frac{N}{2}-\frac{1}{Z_t}(Z_6Z_{10}) \qquad (32)$$

$$Z_2=\frac{K}{2}-\frac{1}{Z_t}(Z_6Z_7) \qquad (33)$$

$$Z_3=\frac{O}{2}-\frac{1}{Z_t}(Z_7Z_8) \qquad (34)$$

Table I. Mesh Currents for Example 2

Load On	Mesh Currents Due to Each Load Current				
	I$_6$	I$_7$	I$_8$	I$_9$	I$_{10}$
I_1...	47.49+j21.02	16.51−j69.02	16.51−j69.02	16.51−j69.02	16.51−j69.02
I_3...	60.20−j131.90	−119.01+j160.45	−60.20+j131.90	−60.20+j131.90	−60.20+j131.90
I_4...	217.30+j143.90	−202.30−j117.92	−202.30−j117.92	−217.30−j143.90	−217.30−j143.90
I_5...	15.69−j14.91	−2.52+j8.38	−2.52+j8.38	−2.52+j8.38	−15.69+j14.91
Total	340.68+j18.11	−307.32−j18.11	−248.51−j46.66	−263.51−j72.64	−276.68−j66.11

Table A

Winding	Kilovolt-Ampere Base	Reactance Per Cent
1 to 2	20,000	14
1 to 3	20,000	20
1 to 4	5,000	12
1 to 5	5,000	15
2 to 3	20,000	16
2 to 4	5,000	10
2 to 5	5,000	11
3 to 4	5,000	8
3 to 5	5,000	8
4 to 5	5,000	6

$$Z_4=\frac{R}{2}-\frac{1}{Z_t}(Z_8Z_9) \qquad (35)$$

$$Z_5=\frac{T}{2}-\frac{1}{Z_t}(Z_9Z_{10}) \qquad (36)$$

$$Z_6=\frac{MQL}{2SP}+\frac{L}{2}\left(\frac{Q}{S}+1\right)+\frac{M}{2}\left(\frac{Q}{P}+1\right) \qquad (37)$$

$$Z_7=\frac{P}{M}Z_6 \qquad (38)$$

$$Z_8=\frac{SP}{QM}Z_6 \qquad (39)$$

$$Z_9=\frac{SP}{QL}Z_6 \qquad (40)$$

$$Z_{10}=\frac{S}{L}Z_6 \qquad (41)$$

$$Z_t=Z_6+Z_7+Z_8+Z_9+Z_{10} \qquad (31)$$

in which the constants are those previously defined.

Comments on Negative Impedance

No practical use can be made of the negative resistances or reactances of transformers as negative impedance by themselves. These negative impedances can be considered as mathematical fictions. They are virtual values. They reproduce faithfully the *terminal* characteristics of the transformer, but they cannot necessarily be applied to internal coils.

Negative resistance may appear in high-efficiency transformers in which the stray impedance losses become appreciable when compared with the low ohmic copper losses. This condition quite often occurs in three-winding transformers and is even more apparent in a five-winding transformer.

The total leakage reactance between a pair of windings cannot be negative, but there is no reason why the mutual effect between circuits for load currents may not be negative, depending on how the leakage field of one interacts with that of the other. In the circuits considered, the individual leakage impedances assigned to a circuit constitute mutual load impedance for the other circuits, and as such may be either positive or negative.

The total copper losses, including stray impedance losses, are correctly represented by the equivalent circuit, whatever the signs of its branches may be; but the simple equivalent circuit provides no means of distributing the losses within the transformer windings. Some of these losses are not in the windings at all, but in the clamps, core end plates, and tank, and therefore the conductor heating cannot be based on the losses of the equivalent circuit. However, the total impedance losses causing the heating of the oil are given by the equivalent circuit.

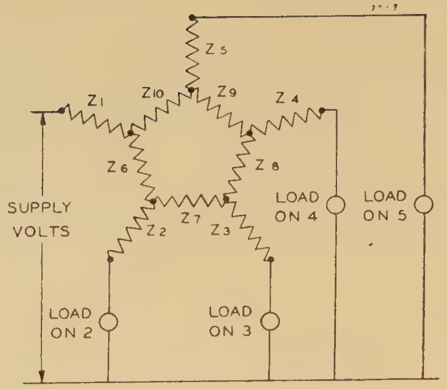

Figure 3. Five-winding transformer equivalent circuit

Examples

The foregoing theory can be applied to any five-winding transformer in a manner similar to that encountered in any electrical network. The only requirement is that certain data relating the actual circuits must be available either from measurements or from design calculations in order that the equivalent circuit may be set up. With the aid of the equations developed herein, each unknown can be evaluated singly. It is evident that all equations, depend upon determining the mesh impedances in the order Z_6, Z_7, Z_8, Z_9, and Z_{10}.

The application of these principles to two types of problems will be outlined in detail. Regardless of the degree of simplicity or complexity, the same precise rules must be followed. The importance of observance of the sign, positive or negative, accompanying each numerical value cannot be emphasized too strongly.

The first example demonstrates the use of this method of solution when only the reactance component of impedance is important. A second example treats resistance and reactance with equal importance, as is common when complex quantities represent the impedance units. Only a single-phase solution need be made for any balanced three-phase bank; however, if unlike transformers are involved, a solution for each phase must be made.

Example 1. Reactance Components Only. A five-winding transformer has the reactance between windings related to the kilovolt-ampere base indicated for each as shown in Table A. When all reactances are translated to a common kilovolt-ampere base, 20,000 in this case, the reactance values become those indicated in Table B. Then

$$K=Z_{23}+Z_{12}-Z_{13}=16+14-20=10$$

$$L=Z_{13}+Z_{24}-Z_{14}-Z_{23}=20+40-48-16$$
$$=-4$$

$$M=Z_{14}+Z_{25}-Z_{15}-Z_{24}=48+44-60-40$$
$$=-8$$

$$N=Z_{12}+Z_{15}-Z_{25}=14+60-44=30$$

$$O=Z_{34}+Z_{23}-Z_{24}=32+16-40=8$$

$$P=Z_{24}+Z_{35}-Z_{34}-Z_{25}=40+32-32-44$$
$$=-4$$

$$Q=Z_{13}+Z_{25}-Z_{12}-Z_{35}=20+44-14-32$$
$$=18$$

$$R=Z_{45}+Z_{34}-Z_{35}=24+32-32=24$$

$$S=Z_{14}+Z_{35}-Z_{13}-Z_{45}=48+32-20-24$$
$$=36$$

$$T=Z_{45}+Z_{15}-Z_{14}=24+60-48=36$$

The mesh links are

$$Z_6=\frac{MQL}{2SP}+\frac{L}{2}\left(\frac{Q}{S}+1\right)+\frac{M}{2}\left(\frac{Q}{P}+1\right)$$
$$=\frac{(-8)\times18\times(-4)}{2\times36\times(-4)}+\frac{(-4)}{2}\left(\frac{18}{36}+1\right)+$$
$$\frac{(-8)}{2}\left(\frac{18}{-4}+1\right)=9$$

$$Z_7=\frac{P}{M}\quad Z_6=\frac{(-4)}{(-8)}\times9=4.5$$

$$Z_8=\frac{SP}{QM}\quad Z_6=\frac{36\times(-4)}{18\times(-8)}\times9=9$$

$$Z_9=\frac{SP}{QL}\quad Z_6=\frac{36\times(-4)}{18\times(-4)}\times9=18$$

$$Z_{10}=\frac{S}{L}\quad Z_6=\frac{36}{(-4)}\times9=-81$$

$$Z_t=Z_6+Z_7+Z_8+Z_9+Z_{10}$$
$$=9+4.5+9+18-81=-40.5$$

and the branches are

$$Z_1=\frac{N}{2}-\frac{1}{Z_t}(Z_6Z_{10})$$
$$=\frac{30}{2}-\frac{1}{(-40.5)}(9\times-81)=-3$$

$$Z_2=\frac{K}{2}-\frac{1}{Z_t}(Z_6Z_7)$$
$$=\frac{10}{2}-\frac{1}{(-40.5)}(9\times4.5)=6$$

$$Z_3=\frac{O}{2}-\frac{1}{Z_t}(Z_7Z_8)$$
$$=\frac{8}{2}-\frac{1}{(-40.5)}(4.5\times9)=5$$

$$Z_4=\frac{R}{2}-\frac{1}{Z_t}(Z_8Z_9)$$
$$=\frac{24}{2}-\frac{1}{(-40.5)}(9\times18)=16$$

$$Z_5=\frac{T}{2}-\frac{1}{Z_t}(Z_9Z_{10})$$
$$=\frac{36}{2}-\frac{1}{(-40.5)}(18\times-81)=-18$$

Example 2. Determination of Current and Voltage Relations for a Five-Winding Transformer Under Load Conditions. A single-phase five-winding transformer has the characteristics shown in Table C and

Table B

Winding	Reactance Per Cent
1 to 2	14
1 to 3	20
1 to 4	48
1 to 5	60
2 to 3	16
2 to 4	40
2 to 5	44
3 to 4	32
3 to 5	32
4 to 5	24

impedance data given in Table D. The circuit diagram of this transformer is shown in Figure 2.

The following information about this transformer installation is required:

1. The equivalent circuit.

2. The load on winding 3 and its power factor when power is supplied from a transmission line to winding 2, maximum capacity 10,000 kva, and from a local generating station to winding 3. The load is on windings 1 and 5, and a synchronous condenser is on winding 4 for power-factor correction. The loads and their power factor are shown in Table E. In order to operate the transmission line connected to winding 2 at its optimum value, an overload of 1,500 kva is necessary on winding 4 from the synchronous condenser.

3. The voltage at each transformer terminal if it is desirable to maintain rated line voltage on winding 3, assuming load ratio control equipment is available to distribute the loads as given in 2.

Solution of 1. The equivalent circuit. The impedance when referred to a common base of kilovolt-amperes of 30,000 is:

$$Z_{12}=\left(\frac{50\times100}{10{,}000}+j20\right)3=1.5+j60$$

$$Z_{13}=\left(\frac{150\times100}{30{,}000}+j20\right)=0.5+j20$$

$$Z_{14}=\left(\frac{30\times100}{7{,}500}+j10\right)4=1.6+j40$$

$$Z_{15}=\left(\frac{15\times100}{5{,}000}+j15\right)6=1.8+j90$$

$$Z_{23}=\left(\frac{40\times100}{10{,}000}+j10\right)3=1.2+j30$$

$$Z_{24}=\left(\frac{52.5\times100}{7{,}500}+j5\right)4=2.8+j20$$

$$Z_{25}=\left(\frac{20\times100}{5{,}000}+j15\right)6=2.4+j90$$

Table C

Winding	Kilovolt-Amperes	Circuit Voltage
1	30,000	230,000 Wye
2	10,000	115,000 Wye
3	30,000	22,000 delta
4	7,500	13,800 delta
5	5,000	34,500 delta

$$Z_{34}=\left(\frac{45\times100}{7{,}500}+j10\right)4=2.4+j40$$

$$Z_{35}=\left(\frac{30\times100}{5{,}000}+j10\right)6=3.6+j60$$

$$Z_{45}=\left(\frac{25\times100}{5{,}000}+j5\right)6=3.0+j30$$

from which

$$K=Z_{23}+Z_{12}-Z_{13}=(1.2+j30)+(1.5+j60)-(0.5+j20)=2.2+j70$$

$$L=Z_{13}+Z_{24}-Z_{14}-Z_{23}=(0.5+j20)+(2.8+j20)-(1.6+j40)-(1.2+j30)=0.5-j30$$

$$M=Z_{14}+Z_{25}-Z_{15}-Z_{24}=(1.6+j40)+(2.4+j90)-(1.8+j90)-(2.8+j20)=-0.6+j20$$

$$N=Z_{12}+Z_{15}-Z_{25}=(1.5+j60)+(1.8+j90)-(2.4+j90)=0.9+j60$$

$$O=Z_{34}+Z_{23}-Z_{24}=(2.4+j40)+(1.2+j30)-(2.8+j20)=0.8+j50$$

$$P=Z_{24}+Z_{35}-Z_{34}-Z_{25}=(2.8+j20)+(3.6+j60)-(2.4+j40)-(2.4+j90)=1.6-j50$$

$$Q=Z_{13}+Z_{25}-Z_{12}-Z_{35}=(0.5+j20)+(2.4+j90)-(1.5+j60)-(3.6+j60)=-2.2-j10$$

$$R=Z_{45}+Z_{34}-Z_{35}=(3.0+j30)+(2.4+j40)-(3.6+j60)=1.8+j10$$

$$S=Z_{14}+Z_{35}-Z_{13}-Z_{45}=(1.6+j40)+(3.6+j60)-(0.5+j20)-(3.0+j30)=1.7+j50$$

$$T=Z_{45}+Z_{15}-Z_{14}=(3.0+j30)+(1.8+j90)-(1.6+j40)=3.2+j80$$

Substituting these values into the equations for the mesh impedances, 37 to 41, and 31, these result:

$$Z_6=\frac{MQL}{2SP}+\frac{L}{2}\left(\frac{Q}{S}+1\right)+\frac{M}{2}\left(\frac{Q}{P}+1\right)$$

$$=\frac{(-0.6+j20)(-2.2-j10)(0.5-j30)}{2(1.7+j50)(1.6-j50)}+\frac{(0.5-j30)}{2}\left(\frac{-2.2-j10}{1.7+j50}+1\right)+\frac{(-0.6+j20)}{2}\left(\frac{-2.2-j10}{1.6-j50}+1\right)$$

$$=0.697-j1.19$$

$$Z_7=\frac{P}{M}\quad Z_6=\frac{1.6-j50}{-0.6+j20}(0.697-j1.19)=-1.749+j2.97$$

$$Z_8=\frac{SP}{QM}\quad Z_6=\frac{(1.6-j50)(1.7+j50)}{(-2.2-j10)(-0.6+j20)}(0.697-j1.19)=11.05-j12.75$$

$$Z_9=\frac{SP}{QL}\quad Z_6=\frac{(1.7+j50)(1.6-j50)}{(-2.2-j10)(0.5-j30)}(0.697-j1.19)=-7.48+j8.40$$

$$Z_{10}=\frac{S}{L}\quad Z_6=\frac{1.7+j50}{0.5-j30}(0.697-j1.19)=-1.06+j2.04$$

$$Z_t=Z_6+Z_7+Z_8+Z_9+Z_{10}=(0.697-j1.19)+(-1.749+j2.97)+(11.05-j12.75)+(7.48+j8.40)+(-1.06+j2.04)=1.46-j0.53.$$

It follows that the branch impedances from equations 32 to 36 inclusive are:

$$Z_1=\frac{N}{2}-\frac{1}{Z_t}(Z_6Z_{10})=\frac{0.9+j60}{2}-\frac{(0.697-j1.19)(-1.06+j2.04)}{(1.46-j0.53)}=0.03+j28.03$$

$$Z_2=\frac{K}{2}-\frac{1}{Z_t}(Z_6Z_7)=\frac{2.2+j70}{2}-\frac{(0.697-j1.19)(-1.749+j2.97)}{(1.46-j0.53)}=0.61+j31.98$$

$$Z_3=\frac{O}{2}-\frac{1}{Z_t}(Z_7Z_8)=\frac{0.8+j50}{2}-\frac{(-1.749+j2.97)(11.05-j12.75)}{(1.46-j0.53)}=1.28-j12.45$$

$$Z_4=\frac{R}{2}-\frac{1}{Z_t}(Z_8Z_9)=\frac{1.8+j10}{2}-\frac{(11.05-j12.75)(-7.48+j8.40)}{(1.46-j0.53)}=27.43-j117.70$$

$$Z_5=\frac{T}{2}-\frac{1}{Z_t}(Z_9Z_{10})=\frac{3.2+j80}{2}-\frac{(-7.48+j8.40)(-1.06+j2.04)}{(1.46-j0.53)}=1.87+j56.65$$

Solution of 2. The Power Supplied to Winding 3 and Its Power Factor When the Fluctuating Load Is Absorbed by the Generating Station on Winding 3. In large power transformers the effect of losses and reactance on input energy and power factor as compared with the output and its power factor is quite small, sufficiently so, to justify its omission in the interest of simplification.

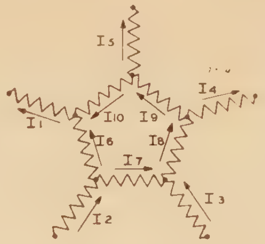

Figure 4. Assumed directions of currents for example 2

The second source of power, that of winding 3, is regarded as a negative load.

Kilovolt-ampere load on winding 1
=24,000 (0.8−j0.6)=19,200−j14,400

Kilovolt-ampere load on winding 2
=−10,000 (1.0+j0)=−10,000+j0

Kilovolt-ampere load on winding 4
=9,000 (0.5+j0.866)=4,500+j7,794

Kilovolt-ampere load on winding 5
=4,500 (0.9−j0.435)=3,950−j1,958

Kilovolt-amperes supplied by winding 3
=17,650−j8,564
=19,625 kva at 89.9 per cent power factor

Table D

Winding	Kilovolt-Ampere Base	Copper Loss Kilowatts	Reactance Per Cent
1 to 2	10,000	50	20
1 to 3	30,000	150	20
1 to 4	7,500	30	10
1 to 5	5,000	15	15
2 to 3	10,000	40	10
2 to 4	7,500	52.5	5
2 to 5	5,000	20	15
3 to 4	7,500	45	10
3 to 5	5,000	30	10
4 to 5	5,000	25	5

Solution of 3. The Voltage at Each Transformer Line Terminal When It Is Desired to hold 22,000 Volts Across Winding 3 With the Local Generator. Since all values are expressed in terms of per cent with the exception of the loads, it is advantageous to convert all loads to a per cent basis using 30,000 kva as the reference. The per cent load on the windings is:

$$\text{On winding 1 } (19{,}200-j14{,}400)\,\frac{100}{30{,}000}= 64-j48=I_1$$

$$\text{On winding 2 } (10{,}000+j0)\,\frac{100}{30{,}000}= 33.33+j0=I_2$$

$$\text{On winding 3 } (17{,}650-j8{,}564)\,\frac{100}{30{,}000}= 58.81-j28.55=I_3$$

$$\text{On winding 4 } (4{,}500+j7{,}794)\,\frac{100}{30{,}000}= 15.00+j25.98=I_4$$

$$\text{On winding 5 } (3{,}950-j1{,}958)\,\frac{100}{30{,}000}= 13.17-j6.53=I_5$$

The division of load by the various mesh circuits is arrived at by solving for the currents that exist, taking each load singly. After all loads are properly proportioned according to the rules of parallel circuits, the resultant current in each mesh link is found by a simultaneous superposition. Winding 2 is used as the source in all load-division computations. The direction of currents has been assumed as in Figure 4 in which the current in link 7 due to load on winding 1 is:

$$I_7=I_8=I_9-I_{10}=\frac{Z_6}{Z_t}\,I_1$$
$$=\frac{0.697-j1.19}{1.46-j0.53}(64-j48)=16.51-j69.02$$

Also

$$I_6=I_1-I_7=(64-j48)-(16.51-j69.02)$$
$$=47.49+j21.02$$

Table E

Winding	Kilovolt-Amperes	Per Cent
1	24,000	80 lagging
4	9,000	50 leading
5	4,500	90 lagging

Current in link 6 due to load on winding 3 is:

$$I_6=-I_{10}=-I_9=-I_8=$$
$$\frac{Z_7}{Z_t}\,I_3=\frac{-1.749+j2.97}{1.46-j0.53}\times$$
$$(-58.81+j28.55)=60.20-j131.90$$

Also

$$I_7=-I_3+I_8=(-58.81+j28.55)+$$
$$(-60.20+j131.90)=-119.01+j160.45$$

Current in link 6 due to load on winding 4 is:

$$I_6=-I_{10}=-I_9=\frac{Z_7+Z_8}{Z_t}\,I_4$$
$$=\frac{(-1.749+j2.97)+(11.05-j12.75)}{1.46-j0.53}\times$$
$$(15.0+j25.98)=217.30+j143.90$$

Also

$$I_7=I_8=I_4-I_6=(15.0+j25.98)-(217.30+$$
$$j143.90)=-202.30-j117.92$$

Current in link 7 due to load on winding 5 is:

$$I_7=I_8=I_9=\frac{Z_6+Z_{10}}{Z_t}\,I_5$$
$$=\frac{(0.697-j1.19)+(-1.06+j2.04)}{1.46-j0.53}\times$$
$$(13.17-j6.53)=-2.52+j8.38$$

Also

$$I_6=-I_{10}=I_5-I_7=(13.17-j6.53)-$$
$$(-2.52+j8.38)=15.69-j14.91$$

The superposition of all currents is most conveniently handled in a table similar to Table I.

The per cent voltage at the terminals of any winding is determined by subtracting the per cent voltage drop through the circuit from the 100 per cent value assigned to the winding used as a reference.

The per cent voltage drop in the branches and links of the mesh is:

$$I_1Z_1=(64-j48)(0.03+j28.03)\,\frac{1}{100}$$
$$=13.47+j17.93$$

$$I_2Z_2=(33.33+j0)(0.61+j31.98)\,\frac{1}{100}$$
$$=0.20+j10.66$$

$$I_3Z_3=(58.81-j28.55)(1.28-j12.45)\,\frac{1}{100}$$
$$=2.80-j7.69$$

$$I_4Z_4=(15.0+j25.98)(27.43-j117.70)\frac{1}{100}$$
$$=34.71-j10.52$$

$$I_5Z_5=(13.17-j6.53)(1.87+j56.65)\,\frac{1}{100}$$
$$=3.95+j7.34$$

$$I_6Z_6=(340.68+j18.11)(0.697-j1.19)\frac{1}{100}$$
$$=2.59-j3.93$$

$$I_7Z_7=(-307.32-j18.11)(-1.749+j2.97)\frac{1}{100}$$
$$=5.91-j8.81$$

$$I_8Z_8=(-248.51-j46.66)(11.05-j12.75)\frac{1}{100}$$
$$=-33.42+j26.54$$

$$I_9Z_9=(-263.51-j72.64)(-7.48+j8.40)\frac{1}{100}$$
$$=25.81-j16.70$$

$$I_{10}Z_{10}=(-276.68-j66.11)(-1.06+j2.04)\frac{1}{100}$$
$$=4.28-j4.94$$

The voltage on the generator bus is held at 22,000 volts, which is our 100 per cent base and the point from which all voltages are calculated.

$$\text{Per cent } V_1=100-(I_3Z_3-I_7Z_7+I_6Z_6+I_1Z_1)$$
$$=100-[(-2.80-j7.69)-$$
$$(5\ 91-j8.81)+(2.59-j3.93)+$$
$$(13.47+j17.93)]=92.65-j15.03$$

$$\text{and } V_1=230{,}000\sqrt{92.65^2+15.03^2}$$
$$=216{,}000 \text{ volts}$$

$$\text{Per cent } V_2=100-(I_3Z_3-I_7Z_7-I_2Z_2)$$
$$=100-[(-2.80-j7.69)-(5.91-j8.81)-$$
$$(0.20+j10.66)]=108.51-j9.54$$

$$\text{and } V_2=115{,}000\ \sqrt{108.51^2+9.54^2}$$
$$=125{,}400 \text{ volts}$$

$$\text{Per cent } V_4=100-(I_3Z_3+I_8Z_8+I_4Z_4)$$
$$=100-[(-2.80-j7.69)+(-33.42+$$
$$j26.54)+(34.71-j10.52)]=101.52-j8.33$$

$$\text{and } V_4=13{,}800\sqrt{101.52^2+8.33^2}$$
$$=14{,}070 \text{ volts}$$

$$\text{Per cent } V_5=100-(I_3Z_3+I_8Z_8+I_9Z_9+I_5Z_5)$$
$$=100-[(-2.80-j7.69)+(-33.42$$
$$+j26.54)+(25.81-j16.70)+(3.95+$$
$$j7.34)]=109.47-j9.49$$

$$\text{and } V_5=34{,}500\sqrt{109.47^2+9.49^2}$$
$$=37{,}930 \text{ volts}$$

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Paired-Phase Bus Bars for Large Polyphase Currents

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THE design of efficient conductor systems for large alternating currents has always been a problem, and the problem is especially difficult with large low-voltage polyphase currents, such as are found in feeder distribution systems for industrial plants. Special conductor shapes and arrangements have been proposed in the past and have proved satisfactory for some applications, but all have had their limitations. A new method of arranging bus bars, named paired phasing, has now been developed and has been found to be superior to conventional arrangements for transmitting polyphase currents in the range of 1,000 amperes and above, at 600 volts and less.

The paired-phase arrangement is described in this paper in terms of enclosed bus-bar systems, as most of the experience in the use of the arrangement has been in that connection, but emphasis has been placed on the general characteristics of the arrangement rather than on this specific application. Therefore, the comparisons with conventional arrangements, the basic electrical and mathematical theory, and the construction and performance data will readily suggest the adaptability of paired phasing to other applications where efficient utilization of conductor materials and convenient structure are desired.

Only the three-phase version of the paired-phase arrangement is considered in this paper. It is shown in cross section in Figure 1. There are six conductors arranged in three separate and distinct pairs. The pairs are *a-b*, *b-c*, and *c-a*; that is, the two conductors in each close-spaced pair are connected to different phases. Each of the three phase currents is thus carried by two conductors in parallel. Wide flat bars placed face to face proved to have the most satisfactory conductor shape.

Summary

With the paired-phase arrangement, many of the difficulties associated with the transmission of large alternating currents are minimized. Nonuniform current density, including "skin effect" and "proximity effect," is greatly reduced. This results in low a-c energy losses which combines with effective heat dissipation to produce low and uniform temperature rise. Both resistance and reactance voltage drops are very small, and voltage drop is almost perfectly balanced. Furthermore, the dimensions that produce all these desirable electrical and thermal characteristics lend themselves to convenient structural designs wherein the problems of joining adjacent bus-bar lengths, making tap-offs, support against mechanical and short-circuit forces, and insulation are greatly simplified.

There are several specific features which particularly adapt the paired-phase arrangement to prefabricated enclosed bus-bar systems. Enclosure, of course, impedes heat dissipation, but the low heat generation minimizes this effect. Metallic or magnetic enclosures cause no additional losses and no increase in reactance. Structural designs are possible which facilitate erection in the field and the adaptation of standardized constructions to special installation conditions.

The paired-phase arrangement has several unique properties that distinguish it from previously known arrangements. Its low voltage drop and low losses depend largely upon close intrapair spacing, dimension *s* in Figure 1. The interpair spacing *d*, or different relative pair locations, as illustrated by the three variations of Figure 1, have no appreciable effect upon voltage drop and, therefore, may be varied to obtain low temperature rise and convenient structure. The balanced voltage drop of the paired-phase arrangement does not depend upon geometrical symmetry.

A peculiar inherent characteristic of the arrangement is that the currents in each close-spaced pair of bars are nearly equal and opposite, or somewhat similar to the currents in a single-phase transmission system, and this current division appears to be responsible for paired-phase performance. This single-phase approach also forms the basis of a simple method for calculating paired-phase performance.

Paired-Phase and Conventional Arrangements Tested

The best way to appraise the performance of the paired-phase arrangements is by comparison with commonly used conventional arrangements, shown in Figure 2. In contrast to the paired-phase arrangement, the laminated arrangement has closely spaced bars connected to the same phase, while the interlaced arrangement has the bars evenly spaced and not grouped into distinct pairs.

A large amount of information has been published concerning open laminated bus bars where phases are well separated and no enclosures are present.[1,2] Most of this information is completely inapplicable to conventional enclosed bus-bar assemblies, because additional proximity-effect losses caused by interphase spacings of two or three inches, induced losses in metallic enclosures, and greatly restricted heat dissipation make performance much worse than commonly supposed. Some temperature-rise data for small-size enclosed bus bars and some reactance and resistance data for large-size bus bars in aluminum enclosures are available,[3] but to fully compare and contrast the performance of the paired-phase arrangement new data will be presented upon all of the arrangements shown in Figures 1 and 2.

Designs having two one-fourth- by four-inch round-edge copper bars per phase have been selected for presentation; this size represents medium rather than large current-carrying capacity and emphasizes the need for careful conductor design even in this size. The basic paired-phase theory applies equally well to all conductor sizes commonly used. In general, the relative performance of larger sizes of laminated bus bars is much poorer, while the relative performance of larger sizes of paired-phase and interlaced arrangements is roughly the same.

In order to simplify comparisons, a rectangular 14-gauge steel housing with a cross-section perimeter of 53 inches was used in all tests; the relative breadth and width were varied slightly to accommodate the various arrangements. A housing of this size represents standard practice for the wider-spaced arrangements, but is larger than would ordinarily

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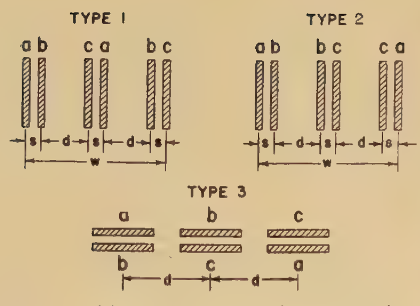


Figure 1. Three variations of the paired-phase bus-bar arrangement

s = intrapair spacing
d = interpair spacing
w = width

be used with the very compact arrangements having a small over-all width, w. Performance of commercial compact arrangements with a smaller housing would, therefore, usually be slightly inferior to that indicated.

The dimension s was kept constant at five-eighths inch during most paired-phase and laminated tests. Comparison of laminated performance with s equal one-half inch, the more conventional dimension, indicated a difference of small magnitude. An intrapair spacing of five-eighths inch represents a more than adequate spacing for practical paired-phase arrangements. The effect of variation of this dimension will, however, be discussed as the test results are analyzed.

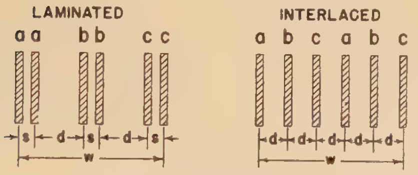


Figure 2. Conventional bus-bar arrangements

All tests were made upon a 40-foot run, comprising four ten-foot sections, in order to minimize end effects. In each case, all bus bars were connected together at one end and low-voltage balanced three-phase 60-cycle current fed in the other end. Voltage measurements were made line to line at the feed-in end of the run to eliminate induced voltages in the potential leads. Power was measured by a special low-voltage wattmeter.

The point-to-point variation in current density in each bus bar was studied by insulated exploring wires cemented to the surfaces of the bars in the manner employed by a number of experimenters.[4–6] The exploring wire measurements also provided a means of analyzing the location of energy losses in greater detail than is possible with a wattmeter, which is suitable only for over-all averages. Each bar was equipped with 16 number 28 wires, eight feet long, spaced at one-half inch intervals around the bar perimeter. The voltage induced in each wire, which is a measure of the current density adjacent to the wire, was measured by a rectangular co-ordinate a-c potentiometer. The total current in each bar was also measured with a magnetic potentiometer or Rogowski coil.[7]

Temperatures were measured by thermocouples. In all tests both the bus bars and housing were painted with a nonmetallic paint to assure maximum and consistent heat radiation. All temperature rises given would be approximately 25 to 35 per cent higher if the bus bars were not painted and 50 to 70 per cent higher if neither housing nor bus bars were painted.

A portion of the research laboratory with a typical test setup is shown in Figure 3. One housing section has been removed so that the interior construction may be seen.

Voltage Drop, Temperature Rise, and Energy Losses

Relative voltage drops and temperature rises are given in Figure 4. Since the voltage drop is unbalanced to some extent in all arrangements, both the maximum line-to-line impedance drop and the minimum line-to-line impedance drop are plotted, with the area between shaded, so that the relative amount of unbalance may be seen. These values are plotted against the over-all width of the bus-bar assembly, w, for all of the vertical-bar arrangements, but it is not intended that comparisons be made only for equal over-all widths. This dimension has no direct significance for the paired-phase type-3 arrangement, but impedance drops and temperature rise for this arrangement are plotted to the same scales so that appraisals may be made. For all arrangements the maximum temperature rise decreases for increased spacings. In the case of the laminated and interlaced arrangements, increased spacing causes both increased voltage drop and increased voltage-drop unbalance; in the paired-phase arrangements increased interpair spacing causes substantially no change in voltage drop but provides better voltage-drop balance.

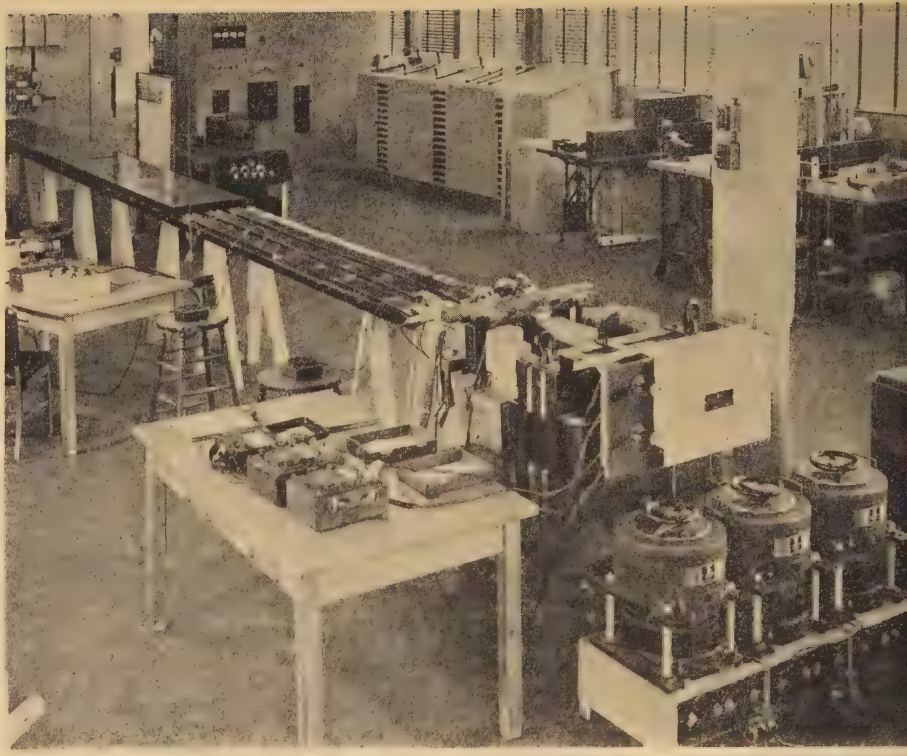


Figure 3. Typical test setup

Impedance drops are separated into average reactance and resistance components in Figures 5 and 6, respectively. The reactance curves in particular show the independence of paired-phase characteristics upon relative pair position. The resistance curves show that both the paired-phase and interlaced arrangements have low average a-c resistance. This factor will be considered in more detail later in connection with an analysis of energy losses. The effect of intrapair spacing upon paired-phase reactance may be seen in Figure 7. This curve is actually for the type-3 arrangement but applies approximately to any practical paired-phase arrangement where interpair distance is great relative to intrapair distance. It shows that lower reactance if

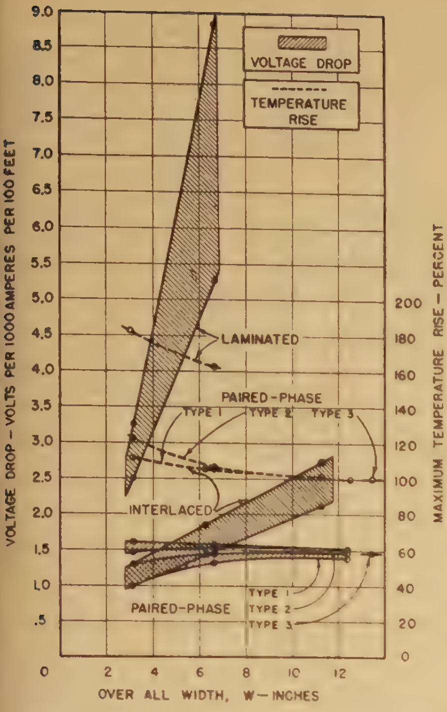


Figure 4. Effect of arrangement and spacing upon line-to-line impedance voltage drop and upon maximum temperature rise

Temperature rises are in per cent of that of the best arrangements. Paired-phase and laminated, $s = {}^{5}/_{8}$ inch. Type 3 paired-phase, $d = 6$ inches. Bar size ${}^{1}/_{4}$ by 4 inches

desired may be achieved by closer intrapair spacing.

Since the paired-phase arrangements with larger interpair spacings (type-1 or 2 arrangement with an over-all width of about six inches or more, or a type-3 arrangement) have superior characteristics from all considerations, there appears to be no practical reason other than space limitations for using smaller interpair spacings. Measurements made with these smaller spacings have been included, however, because of theoretical interest.

The inferior performance of the laminated arrangement needs no further comment, except that one may point out that there may be special instances, such as short connection straps where its use in preference to a more efficient arrangement may be justified by structural limitations.

In comparing the paired-phase and the interlaced arrangements, the practicability of obtaining various bus-bar spacings must be considered. Very close intrapair spacing is practical with paired-phasing, because bars may be offset into the vacant area surrounding each pair of bars at points where connections between adjacent bus-bar lengths or tap-off connections are to be made. Thus a clear space of one-fourth or three-eighths inch between paired-phase bars means a full spacing of that amount throughout the entire bus-bar run. The vacant area surrounding each pair also simplifies the problem of support and insulation. In the interlaced arrangement, because joint and tap-off connections must be made in the confined space between adjacent bars, clearances are necessarily decreased at these points, and insulation and installation problems are complicated. In interlaced arrangements a compromise must be made therefore between electrical properties, temperature rise, and structural complications; that compromise is not necessary with paired-phasing. These facts must be kept in mind when considering the data presented in this paper. For instance, an interlaced bus-bar system with an over-all width, w, of ten inches would have $1^{3}/_{4}$ inches between adjacent bars whereas a type-1 or type-2 paired-phase system of the same over-all width would have approximately $3^{3}/_{4}$ inches between pairs or a type-3 arrangement could be used without changing the electrical characteristics.

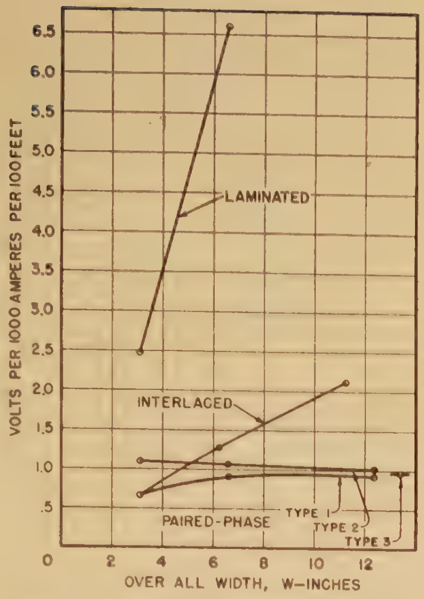


Figure 5. Effect of arrangement and spacing upon line-to-line reactance voltage drop

The relative current density for typical bus-bar arrangements is shown in Figure 8 in which the quantity plotted is the square of the magnitude of the current density. The measurements from the two faces of each bar have been averaged. This method of plotting gives an indication of relative energy losses, which are proportional to the areas under the curves. The more uniform current distribution within each bar and between bars of the paired-phase arrangements is evident. The small interpair effect is indicated by the slight difference in distribution between the two types of paired-phase arrangements. The nonuniform current distribution in both the laminated and the interlaced arrangements is caused by the greater reactance of the outside bars in these arrangements.

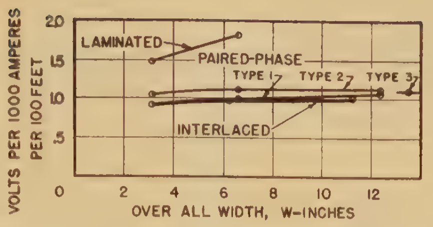


Figure 6. Effect of arrangement and spacing upon line-to-line resistance voltage drop

Copper temperature 75 degrees centigrade

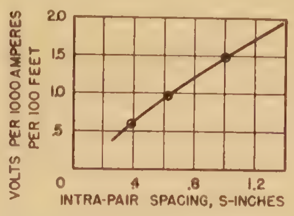


Figure 7. Effect of intrapair spacing, s, upon line-to-line reactance voltage drop of type-3 paired-phase arrangement

The analysis of energy losses in Figure 9 clarifies the performance of the paired-phase arrangement. The most important energy loss is the loss in that individual bar having the greatest energy loss, as this will determine the maximum "hot-spot" temperature rise, and in this respect paired phasing excels. Slightly lower average losses may be secured by using smaller interpair spacings, approaching the interlaced arrangement, but the greater resulting unbalance makes the individual bar losses higher rather than lower. Paired-phase housing losses, as shown in the lower portion of Figure 9, are negligible, and relative pair position has no influence upon these losses. Laminated-arrangement housing losses increase with wide spacing to such an extent that the average total loss increases even though individual bar loss decreases because of lessened proximity effect.

Equal and Opposite Currents Explain Paired-Phase Performance

Paired-phase performance may be understood by examining the distribution of current within the bus bars and the consequent magnetic-field effects in more detail. The magnitude and phase angle of the current density are shown in Figure 10, while the net currents in individual bars are shown in Figure 11. Only one paired-phase arrangement is shown, but

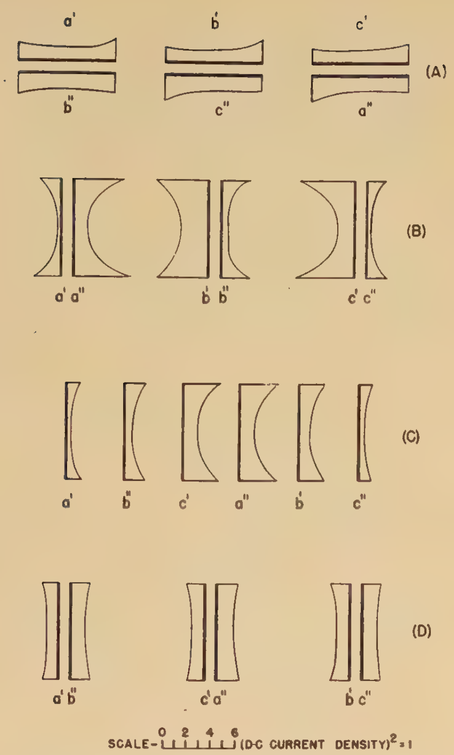


Figure 8. Current density squared in typical arrangements

A. Paired-phase type 3
$d=6$ inches $s=\,^5/_8$ inch

B. Laminated
$d=2^5/_8$ inches $s=\,^5/_8$ inch
$w=6^5/_8$ inches

C. Interlaced
$d=2^1/_4$ inches $w=11^1/_4$ inches

D. Paired-phase type 1
$d=5^1/_4$ inches $s=\,^5/_8$ inch
$w=12^3/_8$ inches

the current distribution is similar in all paired-phase arrangements. There is a marked contrast in this respect between paired-phase and conventional arrangements. It is notable that the phase angle of the current density in the paired-phase arrangements is more uniform within each bar, and the net bar currents are more symmetrically disposed than in the other types of conductor arrangements.

The essence of paired-phase performance is that the currents in each close-spaced pair of bars are essentially equal and opposite. That is, each of the three phase currents (a, b, and c) divides into two component bar currents (a' and a'', b' and b'', c' and c'') which are nearly equal in magnitude and which differ in phase angle by nearly 60 degrees, so that the currents in each close-spaced pair of bars (such as a' and b'', or b' and c'', or c' and a'') differ in phase angle by nearly 180 degrees. The currents in each close-spaced pair are thus essentially similar to the currents in an isolated single-phase transmission system, even though each conductor of the pair is connected in parallel with another conductor in a different pair. This similarity suggests that the paired-phase arrangement may be analyzed by considering each close-spaced pair as a single-phase circuit.

The single-phase transmission system of two wide flat bars closely spaced face to face carrying equal and opposite currents has been analyzed both experimentally and mathematically.[9–12] It has been shown to be a very efficient conductor arrangement. The current density is very uniform, and consequently the a-c resistance is low. For instance, an isolated one-fourth- by four-inch copper bar will have an a-c to d-c resistance ratio of approximately 1.16, but the resistance of a bar of the same size with a similar return conductor five-eighths-inch away, center to center, is less than 1.02. The reactance of such a close-spaced pair is also very small. This means not only low-reactance voltage drop but small external magnetic field. In other words, the magnetic fields of the two equal and opposite currents cancel or neutralize each other. This explains the small interpair proximity effect and the negligible effect of magnetic or metallic surroundings upon either reactance or energy losses in the paired-phase arrangement.

The single-phase pair of closely spaced flat bars has many advantages over other low-loss single-phase arrangements such as concentric tubes. These advantages include the full exposure of both bars for heat dissipation, joining, support, insulation, and tap-offs. This single-phase arrangement has not been used generally in the past, but the recent advent of suitable commercial designs has led to the adoption of this arrangement for the service of large single-phase loads, with very satisfactory results.[13,14]

It is certainly unexpected and seems almost paradoxical that a most efficient method of transmitting large low-voltage polyphase currents should be one wherein the power is, in effect, transmitted single phase; but a comparison of paired phasing with conventional methods, considering all electrical, thermal, and structural factors, indicates that this is indeed true.

The beneficial results of the approximately equal and opposite currents in each close-spaced pair are further emphasized by the consideration of a condition where such a current division does not exist. The average ratio of a-c to d-c loss of the three-phase paired-phase arrangements (with larger interpair spacings) approximates 1.30 (see Figure 9), even though the current density within each bar is very uniform, because each bar carries nearly $1/\sqrt{3}$ rather than one half of the phase current. Losses are proportional to the square of the current: $(1/\sqrt{3})^2/(^1/_2)^2=1.33$. Conventional theory would suggest, therefore, that better performance would be obtained if the current in each bar could be made one half of the phase current, or, in other words, if the phase angle between the two currents in each close-spaced pair could be made 120 degrees instead of nearly 180 degrees, and the phase angle between the two currents of each phase, zero degrees instead of nearly 60 degrees. Such a condition may be produced by making a transposition at the mid-point of a bus-bar run, as shown in Figure 12, wherein each bar is paired with a different phase in each half of the run; for example, the a' bar is paired with a b-phase bar (b'') in the first half of the run and with a c-phase bar (c'') in the second half. The resultant performance is not, however, as might be expected:

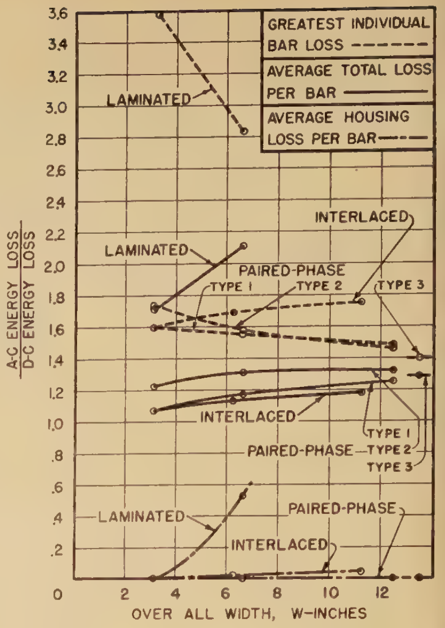


Figure 9. Analysis of energy losses

Loss in individual bar measured by exploring wires. Average total loss measured by wattmeter. Housing loss is difference between average total loss and average bar loss measured by exploring wires (not shown)

Temperature rise	increased 31 per cent
Voltage drop	badly unbalanced and increased 217 per cent
Average energy loss	increased 54 per cent
Housing energy loss	becomes appreciable

In short, such a condition is undesirable, and all the desirable properties of the paired-phase arrangement are lost. This can be explained by the fact that there are no longer equal and opposite currents to cause a cancellation of magnetic effects in each close-spaced pair, and consequently interpair influence become

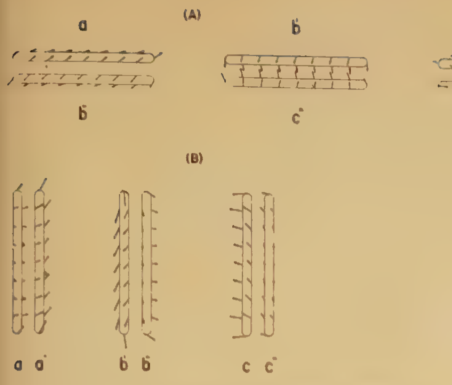


Figure 10. Details of current density

A. Paired-phase type 3 (same dimensions as Figure 8A)

B. Laminated (same dimensions as Figure 8B)
Each vector shows magnitude and phase angle of current density at origin of vector

appreciable. This large interpair proximity effect is shown by the distorted current-density measurements in Figure 13.

Varying the phase angle between the currents in an isolated pair of nonenclosed bars further corroborates the effect of transposition. Test results shown in Figure 14 indicate that even under this simplified condition losses are least when the currents differ in phase angle by 180 degrees. Losses when the currents differ in phase angle by 120 degrees (which represents the condition with transposition) are intermediate between the paired-phase condition and the zero-degree phase-angle condition, corresponding to the laminated arrangement.

Calculation of Paired-Phase Bus Bars

The electrical characteristics of the paired-phase arrangement, including voltage drop and current distribution, are readily figured by electrical theory. A mathematical analysis of a simplified paired-phase arrangement is given in the appendix. These calculations indicate that the division of current between conductors in the paired-phase arrangement is not dependent upon any particular conductor shape but largely upon the grouping of the phases into pairs. Of course the conductor shape influences the distribution of current within each conductor. The mathematical analysis confirms experimental results in determining that the currents in each close-spaced pair become more nearly similar to single-phase as the interpair distance is increased and in fact approach the single-phase condition as a limit as the distance becomes infinite. This suggests and tests confirm that entirely satisfactory paired-phase performance may be secured when a steel housing completely surrounds each pair of bars.

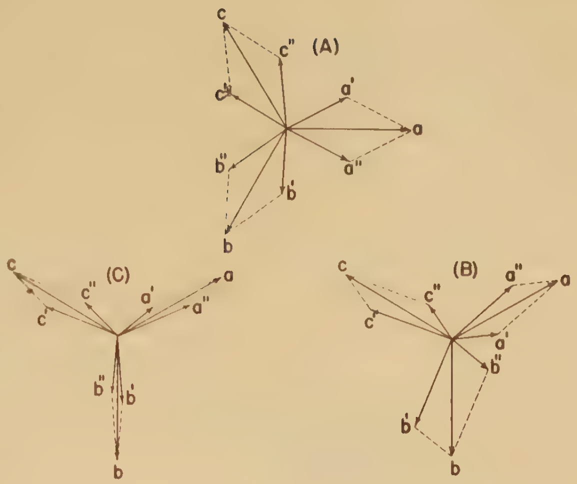
Figure 11 (right). Bar currents and phase currents

A. Paired-phase type 3 (same dimensions as Figures 8A and 10A)

B. Laminated (same dimensions as Figures 8B and 10B)

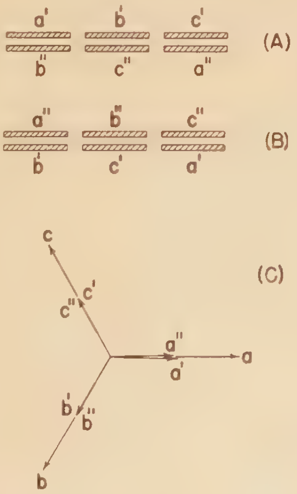
C. Interlaced (same dimensions as Figure 8C)

a′, a″, b′, b″, c′, c″ indicate bar currents
a, b, c indicate phase currents



The usual method of voltage-drop calculation based upon the assumption of equal and inphase currents in all bars connected to each phase will not give correct results when applied to paired-phase arrangements. Voltage drop can be calculated accurately, however, by application of formula 12 or 15 in the appendix. It has been found that for practical dimensions and purposes voltage-drop characteristics may be figured on the assumption of exactly equal and opposite currents in each pair of bars. Both the equivalent single-phase method of formula 15 and the more exact formula 12 give the correct resistance as well as reactance components of voltage drop. Sample calculations are given in the appendix.

Temperature-rise performance is not so readily calculated. Although the current and, hence, losses in each bar can be determined within a few per cent, heat transfer is so complicated by insulation, by proximity of other conductors, and by the enclosure, that tests using three-phase current are necessary for accurate information. Sufficient empirical data of course make possible the prediction of change in performance produced by minor variation in design.



Commercial Design of Paired-Phase Bus Bars

One practical commercial design embodying the paired-phase principle is shown in Figure 15. The flatwise arrangement was chosen because of the advantageous structural features, among which is the simple overlapping type of joining bus bars, insulation, and housing. This arrangement provides complete access to all bus-bar surfaces for cleaning and inspection before joining and complete accessibility of all bolts for maintenance By mounting the bus bars upon flat compression-type porcelain insulators, which in turn are supported on the housing, maximum mechanical and short-circuit strength are obtained. Also the dielectric strength between closely adjacent bus bars is readily improved by the use of a single flat sheet of insulation to supplement the one-fourth-inch minimum air space.

Tests indicate that this insulation sheet increases the temperature rise by less than two per cent. Other measure-

Figure 12 (left). Transposition to change current division

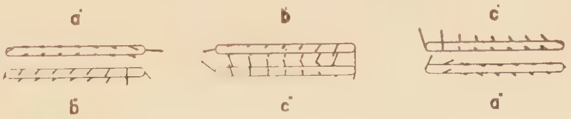
A. Arrangement in one half of bus-bar run

B. Arrangement in other half of bus-bar run. Each bar in A and B insulated except at beginning and end of run, where two bars of each phase are connected together

C. Resulting current division (compare with Figure 11A)

Figure 13. Nonuniform current density produced by transposition

(Compare with Figure 10A)



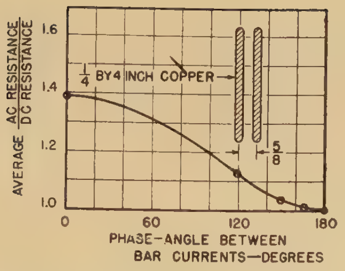


Figure 14. Effect of varying phase angle of currents in an isolated pair of bars

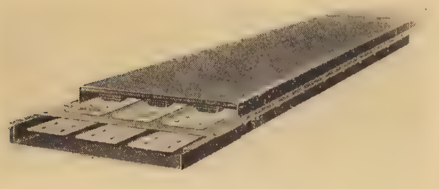


Figure 15. "*Lo-X*" bus duct utilizing paired-phase principle

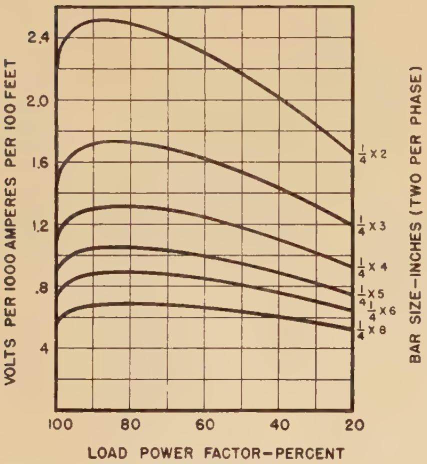


Figure 16. Line-to-line voltage drop of commercial paired-phase bus duct

Unbalance of voltage drop is less than width of curves. Resistance taken at copper temperature of 75 degrees centigrade $s = 1/2$ inch, d = bar width plus 2 inches

ments indicate that with all sizes of bus bars, the hot-spot temperature rise when the entire assembly is mounted edgewise is the same as when it is mounted flatwise; therefore, space limitations will determine the best position for any particular application. The shallow flat shape of the housing proves convenient in installation where close clearances are encountered.

Basic paired-phase principles as just outlined have been utilized in the design of numerous special fittings, such as elbows and tees, transformer taps, load tap-offs, and switchboard connections. Experience has shown that careful attention must be paid to these details if satisfactory over-all performance is to be obtained.

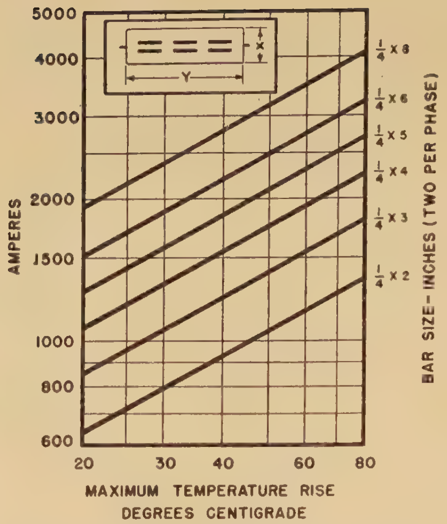


Figure 17. Temperature rise of commercial paired-phase enclosed bus duct indoors in still air

Totally closed steel housing

Bar Size	*X*	*Y*
1/4 by 2	4½	by 14
1/4 by 3	4½	by 17
1/4 by 4	4½	by 20
1/4 by 5	4½	by 23
1/4 by 6	4½	by 26
1/4 by 8	4½	by 32

The temperature-rise and voltage-drop characteristics of a complete line of enclosed bus-bar system utilizing the paired-phase principle are given in Figures 16 and 17. Values are all based on actual tests of complete assemblies, including insulation and housing. These curves indicate the excellent performance that can be obtained with the paired-phase arrangement.

Appendix. Mathematical Analysis of Current-Distribution and Voltage Drop in Paired-Phase Bus Bars

Consider Figure 18. The conductors are of arbitrary shape. The current density is assumed uniform over the cross section of each conductor. The length of the circuit is assumed to be large compared to the distances between conductors. The interpair distance is assumed to be large compared to the intrapair distance, so that as an approximation all distances between conductors not in the same pair may be taken equal. The following symbols will be used:

D_d = geometric mean distance between any two conductors not in the same pair, centimeters
D_s = geometric mean distance between any two conductors in the same pair, centimeters
D_r = geometric mean radius of any conductor, centimeters
r = d-c resistance of any conductor, ohms per centimeter

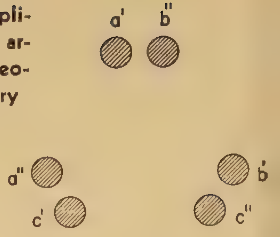


Figure 18. Simplified paired-phase arrangement with geometrical symmetry

E = voltage drop, volts per centimeter, line-to-neutral. (Note that voltage drops in body of this paper are all line to line)
I = current

a', b', c', a'', b'', c'' subscripts refer to conductor quantities. a, b, c subscripts refer to phase quantities.

$\omega = 2\pi$ times frequency in cycles per second

All logarithms are to base ϵ.

Since

$$I_a'+I_a''+I_b'+I_b''+I_c'+I_c''=0 \qquad (1)$$

the voltage drop in phase a may be written: (see reference 19, page 39, equation 55)

$$E_a' = E_a = I_a'r + 2\times10^{-9}j\omega[I_a' \log(1/D_r) + I_b'' \log(1/D_s) + (I_a''+I_b'+I_c'+I_c'')\times \log(1/D_d)] \qquad (2)$$

$$E_a'' = E_a = I_a''r + 2\times10^{-9}j\omega[I_a'' \log(1/D_r) + I_c' \log(1/D_s) + (I_a'+I_b'+I_b''+I_c'')\times \log(1/D_d)] \qquad (3)$$

From symmetry

$$E_b = (-1/2 - j\sqrt{3}/2)E_a \qquad (4)$$

$$E_c = (-1/2 + j\sqrt{3}/2)E_a \qquad (5)$$

$$I_b' = (-1/2 - j\sqrt{3}/2)I_a' \qquad (6)$$

$$I_c' = (-1/2 + j\sqrt{3}/2)I_a' \qquad (7)$$

$$I_b'' = (-1/2 - j\sqrt{3}/2)I_a'' \qquad (8)$$

$$I_c'' = (-1/2 + j\sqrt{3}/2)I_a'' \qquad (9)$$

The solution of the above equations gives

$$I_a' = \left[\frac{1}{2} - \frac{\sqrt{3}}{2}\times \frac{10^{-9}\omega \log(D_d/D_s)}{r+10^{-9}j\omega \log(D_d^3/D_r^2D_s)}\right]I_a \qquad (10)$$

$$I_a'' = \left[\frac{1}{2} + \frac{\sqrt{3}}{2}\times \frac{10^{-9}\omega \log(D_d/D_s)}{r+10^{-9}j\omega \log(D_d^3/D_r^2D_s)}\right]I_a \qquad (11)$$

$$E_a/I_a = \frac{r}{2} + 10^{-9}\omega\left[\frac{1}{2}j \log \frac{D_dD_s}{D_r^2} + \left(\sqrt{3} \log \frac{D_d}{D_s}\right)\left(\frac{\sqrt{3}}{2}\times \frac{10^{-9}\omega \log(D_d/D_s)}{r+10^{-9}j\omega \log(D_d^3/D_r^2D_s)}\right)\right] \text{ ohms per centimeter} \qquad (12)$$

An interesting result is obtained by allowing D_d to become indefinitely large:

$$\text{Lim}_{D_d\to\infty} I_a' = (1/\sqrt{3})(\epsilon^{j30^\circ})I_a \qquad (13)$$

$$\text{Lim}_{D_d\to\infty} I_a'' = (1/\sqrt{3})(\epsilon^{-j30^\circ})I_a \qquad (14)$$

$$\mathrm{Lim}_{D_d \to \infty} E_a/I_a = (4/3)(1/2)\{r + 2 \times 10^{-9} j\omega \log (D_s/D_r)\} \text{ ohms per centimeter} \quad (15)$$

Equations 13, 14, 15, and similar equations for the other phases represent a condition of equal and opposite or single-phase current in each close-spaced pair of bars. The term $[r+2\times10^{-9}j\omega \log (D_s/D_r)]$ in equation 15 is the impedance of one bar of a close-spaced-pair when carrying single-phase current.

Numerical substitution of practical values in equations 10, 11, and 12 shows that large variations in D_d have little effect upon I_a', I_a'', and E_a/I_a. Therefore, it seems reasonable that these equations may be used even when geometrical symmetry, as assumed in deriving these equations, does not exist.

Numerical Example

Consider a type-3 paired-phase arrangement:

Bar size $^1/_4$ by 4 inches
$r = 9.90 \times 10^{-6}$ ohms per foot $= 324 \times 10^{-9}$ ohms per centimeter
$s = {^5/_8}$ inch
$d = 6$ inches

From the formula and curves of reference 17

$D_r = 0.2235\,(4+0.25) = 0.950$ inch $= 2.41$ centimeters
$D_s = 1.372$ inches $= 3.49$ centimeters

GMD of two bars in adjacent pairs (6 inches center to center) $= 5.75$ inches
GMD of two bars in outside pairs (12 inches center to center) $= 11.88$ inches

No readily usable values for GMD of the diagonally located bars are available: therefore, consider diagonal $GMDs$ the same as above. As a working approximation the average may be taken as

$$D_d = \sqrt[12]{(5.75)^8(11.88)^4} = 7.33 \text{ inches} = 18.62 \text{ centimeters}$$

From equation 10

$$I_a' = 0.525\epsilon^{j28.0°} I_a$$

From equation 11

$$I_a'' = 0.591\epsilon^{-j24.6°} I_a$$

From equation 12

$E_a/I_a = (6.17 + j5.71)10^{-6}$ ohms per foot (line to neutral)

From equation 15

$E_a/I_a = (6.58 + j5.65)10^{-6}$ ohms per foot (line to neutral)

By measurement (average of three phases)

$I_a' = 0.544\epsilon^{j27.3°} I_a$
$I_a'' = 0.574\epsilon^{-j25.8°} I_a$
$E_a/I_a = (6.35 + j5.61)10^{-6}$ ohms per foot (line to neutral)

The differences between measured and calculated values are in the directions that would be expected in view of the various assumptions made. The phenomena of Figure 14, neglected in the calculations, makes the phase angle between I_a' and I_a'' more nearly 60 degrees than calculated from equations 10 and 11. The resistance calculated from equation 12 is lower than measured, because slight nonuniform current density in individual bars is neglected. That calculated from equation 15 is higher, because the magnitude of the bar currents is less than the 0.577 I_a assumed by this method. A small correction could be made for nonuniform current density by applying published formulas for skin effect in a single-phase pair of flat bars.[11] Reactances agree within experimental error.

Calculation of impedance by the conventional assumption that $I_a' = I_a''$, vectorially (see reference 17, equations 15 to 20) gives

$E_a/I_a = (4.95 + j8.27)10^{-6}$ ohms per foot (line to neutral)
$E_b/I_b = E_c/I_c = (4.95 + j16.55)10^{-6}$ ohms per foot (line to neutral)
Average $= (4.95 + j13.80)10^{-6}$ ohms per foot (line to neutral)
Measured with $I_a' = I_a''$ (produced by use of a transposition, Figure 12):
Average $= (9.87 + j19.02)10^{-6}$ ohms per foot (line to neutral)

The conventional method of calculation, therefore, does not give correct results for ordinary paired-phase arrangements, because current distribution is not as assumed. On the other hand, when bar currents are as assumed, the current density within each bar is not uniform, and, in addition, the steel housing causes extra losses and higher reactance.

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An Electromechanical Calculator for Directional-Antenna Patterns

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Synopsis: An electromechanical calculator has been designed for the rapid solution of directional-antenna patterns. After the desired antenna parameters have been set up, the machine automatically draws the field-intensity curve to any desired scale on a sheet of polar co-ordinate paper and indicates the rms value for drawing a circle of the equivalent nondirectional pattern.

The fundamental design of the calculator does not limit the number of antennas that can be used in the directional array. As the number of antennas is increased, the greater the degree of control is possible to mould the radiation pattern into the desired shape.

Not only is the horizontal directional field-intensity pattern, such as used in broadcast practice, readily available, but the field-intensity contours at any elevation angle can be drawn with equal ease. The vertical radiation characteristics of the several antennas can be selected at will. This makes it possible to explore the solid contour surface of the whole hemisphere in a relatively short time and also determine the total power radiated from the directional-antenna system. The machine is also suitable for calculating the solid contour surface of the radiation pattern from a parallel horizontal antenna array.

To anyone who has had the task of designing a directional-antenna array, the need of some short cut to reduce the tedium of laborious calculations has been very apparent. To meet this need a number of schemes has been brought forth, ranging from early graphical methods to actual measurements on models, from purely mechanical to purely electrical devices. These devices have varying degrees of usefulness, and all meet to a large degree the requirements of lessening the time required to explore the number of varying parameters necessary to meet some special case.

Extended tables systematizing patterns of two tower arrays have been published.[1–4] These diagrams are very helpful to show trends, even though the designer may in the final analysis have to calculate very precisely the pattern after deciding upon a suitable range of spacings and phasings. The authors felt that a systematization of three tower arrays would be of very real value to the industry. Therefore, in taking up the problem of developing an antenna-pattern calculator, they had in mind not only its immediate use as at first outlined, but also the ultimate accurate and quick development of a great number of patterns. After considering carefully the merits of various methods, it was decided that an electromechanical device could be made to combine the accuracy and simplicity of the mechanical method with the speed and flexibility of the electrical method.

The usual problem in broadcast practice is to mould the radiation pattern into the desired shape to cover the service areas and give the required protection to other radio stations. As a matter of economics, it is desirable to do the job with the minimum number of antennas. With severe requirements, the number of antennas must be increased until the radiation pattern can be moulded into the desired shape. This electromechanical calculator was designed primarily to handle this type of problem. It is not necessary to assume images on the basis of a perfect earth. The only requirement is that the vertical radiation characteristic of the several antennas be known when it is desired to determine the solid contour surface over the whole hemisphere.

It is also possible to solve for the solid contour surface of horizontal radiators located above the surface of the earth. In this case, the horizontal radiation characteristic of the individual antennas in free space must be known. The images in this case must be set up on the machine just as the actual antennas are set up. If the horizontal antennas are perpendicular to and bisected by a vertical plane, then the contours are run at constant angles to this perpendicular plane, just as the various elevation contours are run in the case of vertical antennas. In other words, the contours are run as if the antennas were vertical, and then the contour surface is rotated 90 degrees to locate properly the antennas above the surface of the earth. Only that part of the contour surface is useful that appears above the surface of the earth. For such an array the contour surface will be symmetrical to the vertical plane passing through the center of the antennas.

A more general case is to have horizontal parallel antennas without requiring their centers to bisect a vertical plane. In this case those horizontal antennas and their images, that are not bisected by a vertical plane, must have their phase adjusted by for each contour to correct for the space phase difference. These adjustments of phase are made on the assumption that the observation point is located at infinity, which is the same assumption that is ordinarily used for the antenna spacing. For this more general case the pattern will be unsymmetrical about any vertical plane, and the contour surface will have to be run for both ends of the array.

Design Consideration

In consideration of the problem of systematizing directional-antenna patterns, it is desirable to have considerable flexibility, accuracy, and speed in the machine. Since the time involved to do this job by hand is prohibitive, one can afford to spend considerable time and effort in perfecting a machine to the point that it will give, with sufficient accuracy, this valuable engineering information. The design and development of such a machine, incidentally, involves the solution of a number of rather interesting problems.

The purely mechanical calculator[5,6] has simplicity and accuracy depending upon the preciseness with which the individual mechanical parts are made. However, in its usual forms, it lacks considerable flexibility and speed for calculating all the desired factors involved in a directional pattern. The usual design of these calculators will give the horizontal pattern for three antennas, and, if the antennas are identical, the vertical patterns can be determined without much difficulty. Its operation depends upon one vector being fixed and used as the reference with the vectors of the other two antennas revolving about its ends in the proper fashion. The handling of more than three antennas becomes mechanically involved, because the additional vectors must revolve about the ends of the free-ended vectors. Recording the patterns accurately and automatically with such a device is exceedingly difficult, because the distance between the free-ended vectors must be

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The authors express appreciation to all the operating engineers on the WHK–WCLE staff who have contributed in making the electromechanical calculator a success.

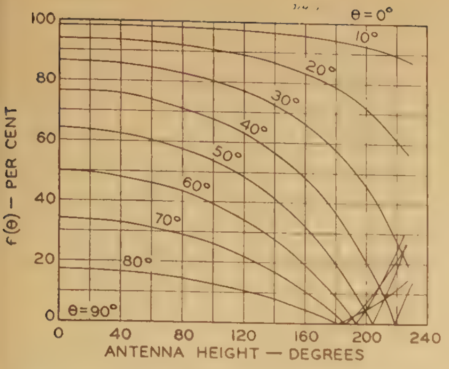


Figure 1. Vertical radiation characteristic as a function of electrical height

measured and recorded accurately at all points. If readings are taken at periodic intervals, these values must again be used in a separate operation to determine the rms value of the pattern. This is a very important quantity if the pattern is to be used for a definite power into the system. Even then there is the question of the power used in the high angle radiation. It usually takes longer to determine the rms value than it does to determine the pattern itself. A more satisfactory method is to use an equation involving Bessel functions to determine the rms value.[7] Appendix I gives the general form of this equation with an illustrative example.

The purely electrical form of calculator may place the directional-antenna pattern on an oscilloscope screen. This scheme is rapid, being visual, can be altered instantaneously by changing the various antenna parameters at will, and is not limited to the number of antennas in the system. The rms value can be read from a meter directly. The limitation of this scheme is primarily lack of accuracy, depending of course upon the electrical design.

The authors have put into operation at the transmitting plant of WHK–WCLE an electromechanical directional-antenna pattern calculator which will automatically draw the horizontal pattern of a four-element array with the possibility of adding as many more elements as desired. This machine will also automatically draw the field-intensity contours at various elevation angles. These data readily yield the vertical pattern in any direction from the antenna system. In addition to this, the machine measures the rms value of the pattern as it is drawn. Hence, the volume of the solid radiation pattern is readily determined by a simple computation. From this information the actual power radiated from the antenna system can easily be computed.

The electromechanical calculator accomplishes its purpose by starting with a simple mechanical system, converting to an electrical system for flexibility, and finally converting back to a mechanical system to draw the curve. The mechanical portion of the calculator consists of a turntable to hold the polar co-ordinate chart paper. This turntable is driven by a variable-speed motor which in turn drives the several mechanical cosine generators. These generators in effect rotate the second, third, and fourth antennas around the first or reference antenna. The mechanical cosine generators are rack-geared to phase-shifting transformers. At this point the mechanical vector is converted into an equivalent electrical vector. All of the electrical vectors can then be added in series and the resultant represents the field intensity in the given direction in space as indicated by the orientation of the turntable carrying the polar co-ordinate chart paper and the elevation angle. An automatic voltagraph converts this electrical magnitude into an equivalent mechanical magnitude to drive the radial inking pen and planimeter. The inking pen draws the pattern while the planimeter measures the rms of the pattern. Usually, after the directional pattern is finished, the rms circle is drawn on the polar co-ordinate chart as additional valuable information.

Generalized Equation

The electromechanical calculator in reality is an equation solver. In this case the equation is of a particular variety, and the machine must be adapted to handle the various parameters in the equation. The method of operation of the machine can best be explained after considering the general nature of the equation to be solved.

For a directional antenna array of n antennas, the generalized equation is

$$\boldsymbol{E}=\sum_{k=1}^{k=n} E_k f_k(\theta)\epsilon^{j\beta_k} \tag{1}$$

where

$\boldsymbol{E}$ = the total effective field-intensity vector for the antenna array with respect to the voltage reference axis

k = the kth antenna in the system

n = the total number of antennas in the complete directional-antenna array

E_k = the horizontal magnitude of the field intensity produced by the kth antenna

$f_k(\theta)$ = vertical radiation characteristic of the kth antenna (unity along the horizon)

θ = elevation angle of the observation point P measured up from the horizon in degrees.

Figure 2. Symbolic representation of the electromechanical antenna-pattern calculator

A—Motor - driven turntable carrying polar co-ordinate chart paper. The orientation of the observer is represented by ϕ

B—Mechanically driven cosine wave generators with adjustments for antenna orientations ϕ_k, spacing S_k and elevation angle θ
$S_k \cos(\phi_k-\phi)\cos\theta$

C—Mechanically driven phase-shifting transformers with adjustments for antenna current phase Ψ_k
$\beta_k=S_k \cos(\phi_k-\phi) \cos\theta\ \Psi_k$

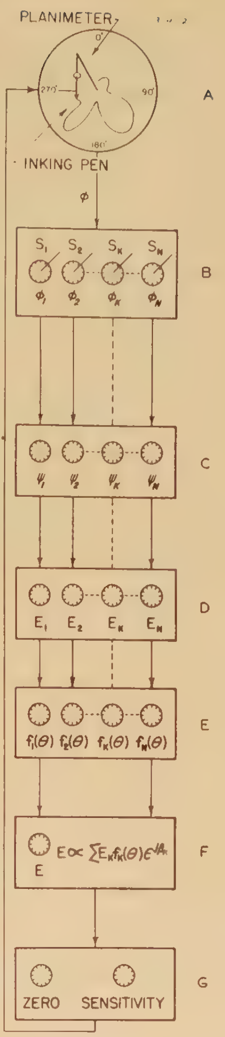
D—Electrical magnitude adjustment of horizontal field intensity $E_k \cdot E_k\epsilon^{j\beta k}$

E—Electrical magnitude to give vertical radiation characteristic $f_k(\theta)$
$E_k f_k(\theta)\epsilon^{j\beta_k}$

F—Summation of electrical vectors and total electrical magnitude control of field intensity *E*

G—Voltagraph converts electric field intensity *E* to an equivalent mechanical magnitude. It is provided with zero and sensitivity adjustments

The output of the voltagraph drives the inking pen to draw the resulting pattern on the polar co-ordinate chart paper. Also, a planimeter is driven to measure the area of the pattern



$\epsilon^{j\beta_k}$ = unity vector determining the direction of the voltage vector of the kth antenna

ϵ = 2.718 . . . the base of natural logarithms

$j=\sqrt{-1}$ imaginary j operator

$$\beta_k=S_k \cos(\phi_k-\phi)\cos\theta+\Psi_k \tag{2}$$

= phase relation of the voltage (or current) in the kth antenna with respect to the voltage reference axis

S_k = electrical length of the spacing of the kth antenna from the space reference point

ϕ_k = true horizontal azimuth, orientation of the kth antenna, with respect to the space reference axis

ϕ = true horizontal azimuth angle of the direction to the observation point P (measured clockwise from true north)

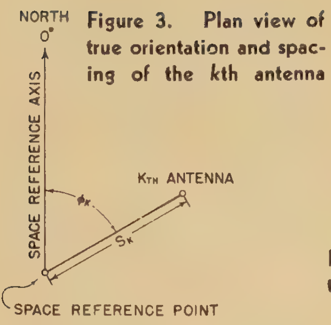


Figure 3. Plan view of true orientation and spacing of the kth antenna

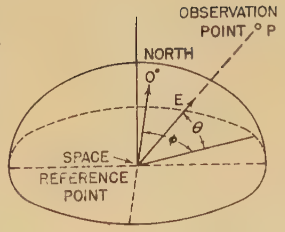


Figure 4. Vector representation of the total field intensity in space

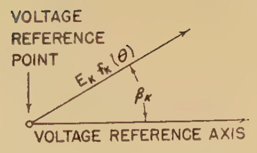


Figure 5. Vector representation of the field intensity of the kth antenna

Ψ_k = electrical phase angle of the voltage (or current) in the *k*th antenna with respect to the voltage reference axis

For a vertical antenna having a sinusoidal current distribution with a current node at the top, the vertical radiation characteristic takes on the form

$$ {}_k(\theta) = \frac{\cos(h \sin\theta) - \cos h}{(1 - \cos h)\cos\theta} \qquad (3)$$

where

h = electrical height of the antenna in degrees.

The other terms are as defined above. The curves in Figure 1 represent the solution of equation 3 over the most useful range of antenna heights.

Machine Solution of Generalized Equation

The electromechanical antenna-pattern calculator solves the generalized equation as shown symbolically in Figure 2. The position of the observer, the placement of the antennas, and the phase of antenna currents determine the direction of the various antenna vector voltages. These adjustments are all mechanical. The magnitude of these vectors are controlled electrically and are added for the total effective field intensity. This field intensity is then converted into an equivalent mechanical magnitude to drive the inking pen and planimeter.

The direction of the inking pen from the zero-degree reference orientation of the turntable represents the orientation ϕ of the observer from the antenna system. The turntable which is driven by a variable-speed motor is geared through clutches to all the cosine wave generators. The orientation of the *k*th antenna, for example, from true north is accomplished on the calculator by locking the turntable so that the inking pen is true north. The *k*th antenna cosine-wave-generator clutch is then released and turned to give the desired orientation ϕ_k in degrees as indicated on the orientation dial. The clutch is then locked into position. The spacing S_k of the *k*th antenna measured from the space reference point is an amplitude adjustment, measured in electrical degrees on the spacing arm, of the cosine wave generator. These two adjustments locate the *k*th antenna as illustrated in Figure 3. The adjustments thus far give the function $S_k \cos(\phi_k - \phi)$ for the horizontal pattern.

The cosine wave generator drives a phase-shifting transformer through a rack and gear. This mechanism is so proportioned that if the observer moves around the antenna, by rotating the turntable, the phase of the current in the *k*th antenna will shift the same number of electrical degrees that the number of electrical degrees from the observer to the antenna changes. The phase of this current is zero in the *k*th antenna when the voltage vector lies along the voltage reference axis. For this condition the distance from the observer to the space reference point is equal to the distance from the observer to the *k*th antenna. The phase Ψ_k is adjusted by a clutch arrangement on the shaft of the phase-shifting transformer. Any electrical phasing measured in degrees can be set mechanically on the phase dial associated with this clutch. With this adjustment added, the function becomes $S_k \cos(\phi_k - \phi) + \Psi_k$ for the horizontal pattern.

All of the mechanically adjusted parameters are represented in equation 2.

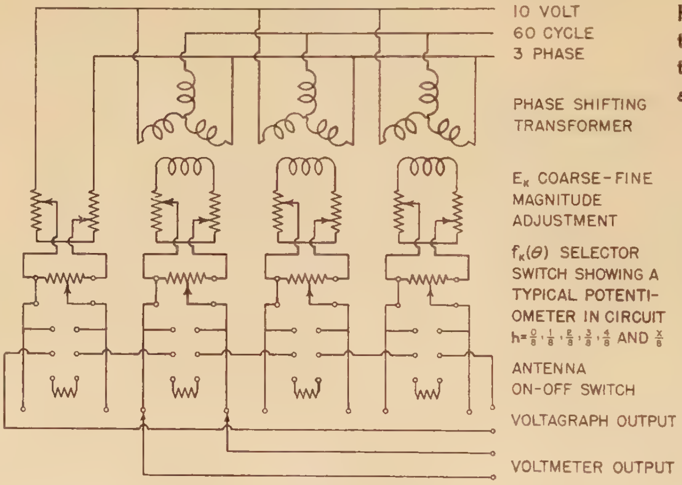

10 VOLT
60 CYCLE
3 PHASE
PHASE SHIFTING TRANSFORMER
E_K COARSE-FINE MAGNITUDE ADJUSTMENT
$f_K(\theta)$ SELECTOR SWITCH SHOWING A TYPICAL POTENTIOMETER IN CIRCUIT
$h = \frac{0}{8}, \frac{1}{8}, \frac{2}{8}, \frac{3}{8}, \frac{4}{8}$ AND $\frac{x}{8}$
ANTENNA ON-OFF SWITCH
VOLTAGRAPH OUTPUT
VOLTMETER OUTPUT

Figure 6 (left). Electric circuit for magnitude control and addition of antenna voltages

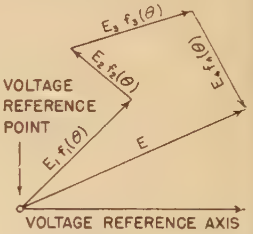


Figure 7. Summation of four antenna field-intensity voltage vectors

Along the horizon $\cos\theta = 1$, and the function described in the above paragraph gives the correct answer. At some elevation angle $\cos\theta$ becomes less than unity; hence this factor can be multiplied by the spacing S_k and an equivalent new closer spacing set up. This is the effect of viewing the antenna system at some elevation angle. The difference in the distance from the observer to the antennas becomes less; hence, the same result is accomplished by staying along the horizon and placing the antennas closer together. Directly over the antenna system all antennas are the same distance; hence, the product $S_k \cos\theta$ must go to zero. The function for any direction is $S_k \cos(\phi_k - \phi)\cos\theta + \Psi_k$.

The direction of the observation point *P* in terms of the true orientation ϕ and elevation angle θ is illustrated in Figure 4. The magnitude of the total effective field intensity *E* is represented along the direction of the line from the space reference point toward the observation point *P* for convenience in drawing the polar charts. The field intensities at a given elevation angle can be duplicated with an equivalent horizontal pattern, using closer antenna spacing and modified field intensities.

The field intensity of the *k*th antenna as given in equation 1 is vectorially represented in Figure 5. The phase angle β_k is controlled by the mechanical adjustments which in turn controls the phase of the output voltage of the phase-shifting transformer. The magnitude of the output voltage E_k is controlled by a potentiometer. See Figure 6. For field-intensity magnitudes at elevation angles, this value must be controlled in accordance with the vertical radiation characteristic $f_k(\theta)$ of the antenna. For convenience in running contours at the various elevation angles, vertical-radiation-characteristic potentiometers are used for antennas having heights in multiples of an eight-wave length plus one position which is adjustable to any other desired vertical pattern. These vertical-pattern potentiometers speed up the work and lessen the chance of making errors. The electric circuit involving these parameters is illustrated in Figure 6.

The total effective field-intensity vector $\boldsymbol{E}$ is the summation of all the vector voltages from the various antennas. In this calculator these voltages are added in series. The resulting summation of the vector voltages for a four-element directional-antenna system is illustrated in Figure 7. Only the angles of these vectors vary as the orientation from the antenna system changes. Both the angle and magnitude changes as the elevation angle θ is varied.

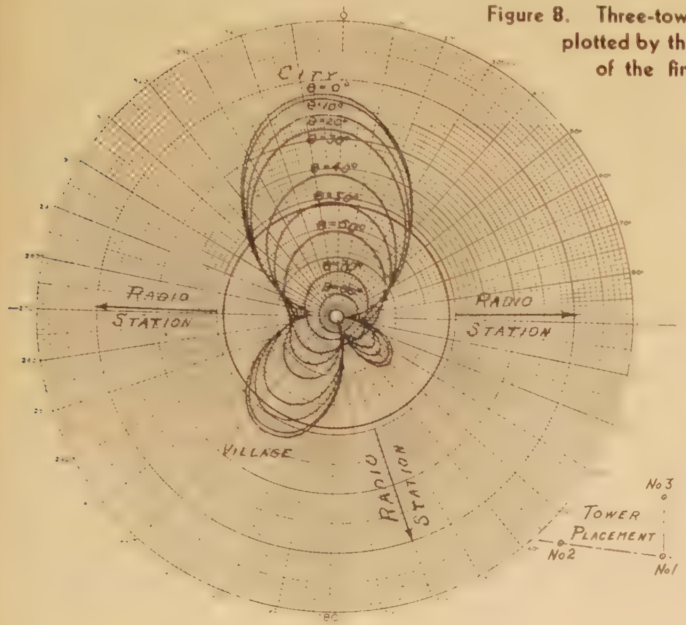


Figure 8. Three-tower solid-contour surface, plotted by the machine, on one sheet of the finely engraved polar coordinate chart paper

Directional - antenna parameters

$\phi_2 = 276$ degrees
$S_2 = 176$ degrees
$\Psi_2 = 296$ degrees
$E_2 = 115$
$E_1 = 168$
$\phi_3 = 0$ degrees
$S_3 = 100$ degrees
$\Psi_3 = 218$ degrees
$E_3 = 115$
$E_{\text{rms}} = 190$
$\phi_4 =$
$S_4 = -$
$\Psi_4 =$
$E_4 = -$
$E_{\max} = 378$
$h_1 = h_2 = h_3 = \frac{\lambda}{4}$

The magnitude E of the total effective field-intensity vector $\boldsymbol{E}$ is now converted into an equivalent mechanical magnitude in order that the radial drawing pen can be driven in such a way as to produce the field-intensity pattern on the polar coordinate chart paper. A rapid self-balancing precision potentiometer circuit was developed to accomplish this result. Adjustments are provided to make zero voltage correspond to the center of the polar co-ordinate chart paper. A sensitivity adjustment makes it possible to select any reasonable scale for a given output voltage magnitude E. Usually in systematizing patterns, the magnitude of the maximum lobe of the pattern is adjusted to fall on a given circle. This usually requires an adjustment of the sensitivity for each pattern.

Enough power is supplied from the balancing mechanism to drive, in addition to the pen, a planimeter which rolls and slides on the chart paper and measures the area of the chart. By means of a table the operator can immediately determine the rms value of the pattern and draw this circle on the chart with the drawing pen controlled by a single antenna which is nondirectional.

Speed

The machine is driven by a variable-speed motor. For simple patterns the speed can be increased somewhat and the machine still maintain the required accuracy. For ordinary systematization work it takes from three to five minutes to complete a pattern. This involves setting the parameters and drawing the pattern and the rms circle. The solid contour surface can be investigated and recorded on the chart paper in less than an hour. This usually involves a total of nine contours placed three to a chart.

Accuracy

The machine is operated from a 220-volt three-phase 60-cycle source at the present time. Line-voltage fluctuations are the greatest source of error. The machine depends upon these voltages remaining constant while the pattern is being run. Originally it was planned to provide an independent source of power free from fluctuations, but this had to be abandoned for the duration of the war.

A number of different makes of phase-shifting transformers was tested, and the best, which are being used in the present machine, showed a maximum error of three per cent. This appeared to be satisfactory for the systematization work. If desired on special jobs, checks can always be made, with a vacuum-tube voltmeter, of all the voltages in the system, to increase the magnitude accuracy to that of the voltmeter. The error in phase angle checks within one degree.

In the systematization work, the antenna orientations ϕ_k are advanced approximately one degree to compensate for the lag in the drawing pen. This lag is a function of the speed of the turntable; hence, by slowing the turntable down it can be practically eliminated.

The precision potentiometer can be adjusted so that the mechanical magnitude tracks with the electrical magnitude and the balance point is always within one per cent of the full-scale value.

The polar planimeter used to measure the area of the charts varies somewhat in its readings but usually can be relied upon to within one per cent.

Chart Paper

Two varieties of special chart paper have been prepared. One is a simple ten-inch circle with crosslines printed on a 12-by-12-inch sheet and is used primarily for the systematization work. Three such charts with patterns were tacked to cover lid of the calculator illustrated in Figure 10. The patterns are all for three tower antennas. The numbers at the bottom completely specify parameters.

The finely engraved chart paper was also prepared on 12-by-12-inch sheets but can be trimmed down to 11 by 12 inches to fold into a standard notebook of $8^1/_2$ by 11 inches. The engravings on this chart consist of a complete circle ten inches in diameter, divided with radials to single degrees and numbered every ten degrees clockwise from the top. The ordinates are divided into ten parts to the inch with heavy markings every half inch. Figure 8 illustrates this chart paper with the horizontal and eight elevation angle contours crowded on one sheet. The legend "Directional-Antenna Parameters" in the upper right-hand corner specifies all the conditions imposed upon the system. The legend in the lower right-hand corner specified the firm name, date, manufacturer and pattern number. This chart paper is now available to the public[8] with or without the blocks in the corners in a light or heavy weight of paper. The chart paper is printed with a brown ink for ease in making photostats or blueprints.

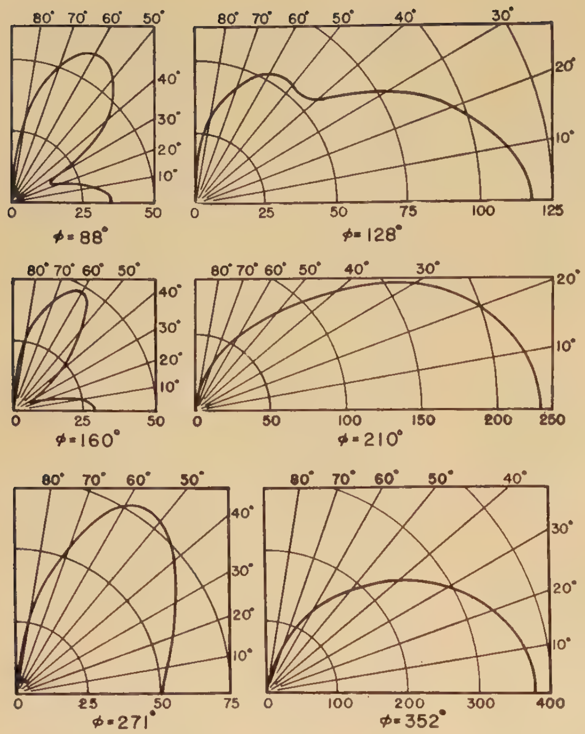


Figure 9 (left). Vertical patterns plotted from the elevation contours of Figure 8

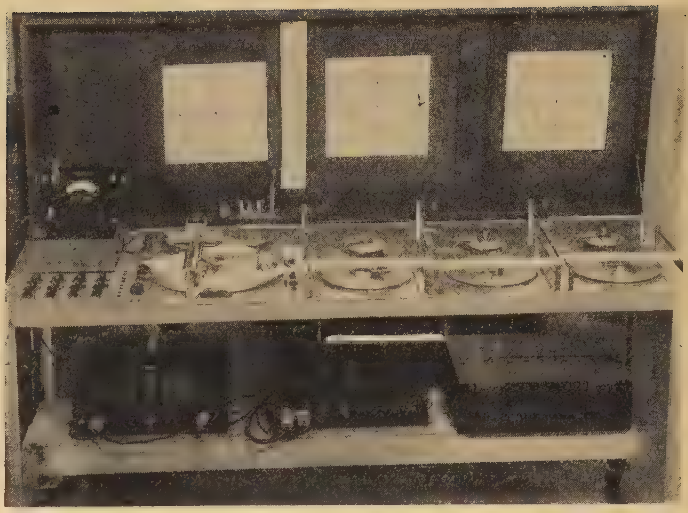


Figure 10. Electromechanical antenna-pattern calculator

Vertical Patterns

Vertical-plane radiation patterns as shown in Figure 9 are readily obtained from the elevation contours in Figure 8. The vertical patterns are important when considering sky-wave interference problems between radio stations.

Illustrations

The complete machine is shown in Figure 10. The magnitude controls are on the left-hand panel, followed by the turntable and cosine wave generator panels. A supply of chart paper is held in a pocket under the table, convenient to the operator. The master-control power switch is on the left-hand front leg. The voltagraph-control amplifiers and power transformers are located on the shelf under the table. The whole unit is on wheels and is provided with a dust cover lid.

Figure 11 is a close-up view showing details of the turntable and one cosine wave generator.

Conclusions

After three months of continuous operation, the machine has proved to be a speedy means of securing directional-antenna patterns. The systematization work which is progressing steadily will be available at a later date.

The machine is extremely valuable when one is searching for a directional-antenna pattern to meet certain specific requirements. Many patterns can be drawn quickly to investigate thoroughly the regions of interest. The systematization of patterns will aid materially in finding these regions of interest.

With care of operation, the results are believed more accurate than can be expected from most directional-antenna installations. The acceptance of such a machine by the profession will remove a considerable amount of the routine work. A few of these machines would perhaps be adequate to supply all the needed patterns; hence, they could be used to offer a directional-antenna pattern service.

Appendix I

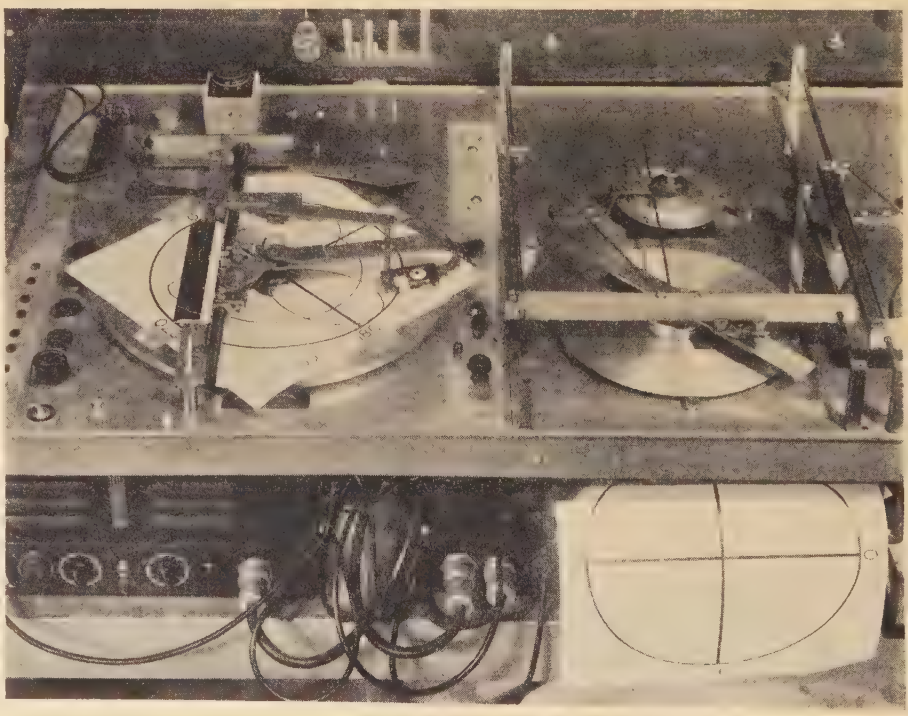
The general form of the equation for determining the root-mean-square value for the directional-antenna pattern involves the product of the field intensity of each antenna multiplied by the field intensity of every antenna in the system. In mathematical form this can be written

$$E_{\mathrm{rms}} = \sqrt{\sum_{p=1}^{p=n} \sum_{q=1}^{q=n} E_p E_q \cos(\Psi_p - \Psi_q) J_0(S_{pq})} \qquad (4)$$

where

E_{rms} = root-mean-square effective field intensity

Figure 11. A close-up view of Figure 10 showing the turntable and one cosine wave generator



p = the pth antenna in the system
q = the qth antenna in the system
n = the total number of antennas in the complete directional antenna array
E_p = horizontal magnitude of the field intensity produced by the pth antenna
E_q = horizontal magnitude of the field intensity produced by the qth antenna
Ψ_p = electrical phase angle of the voltage (or current) in the pth antenna with respect to the voltage reference axis
Ψ_q = electrical phase angle of the voltage (or current) in the qth antenna with respect to the voltage reference axis
$J_0(S_{pq})$ = Bessel function of the first kind and zero order of the spacing S_{pq}, in radians, between the pth and qth antennas

In this equation when $p=q$, the terms are simply the square of the antenna field intensity, because the cosine and Bessel function terms each reduce to unity. For example in a three-tower array the application of equation 4 results in

$$E_{\text{rms}} = \sqrt{\sum_{p=1}^{p=3}\sum_{q=1}^{q=3} E_pE_q \cos(\Psi_p-\Psi_q)J_0(S_{pq})}$$

$$= \sqrt{\begin{aligned}&E_1^2+E_2^2+E_3^2\\ &+2E_1E_2\cos(\Psi_1-\Psi_2)J_0(S_{12})\\ &+2E_1E_3\cos(\Psi_1-\Psi_3)J_0(S_{13})\\ &+2E_2E_3\cos(\Psi_2-\Psi_3)J_0(S_{23})\end{aligned}} \qquad (5)$$

If the parameters for the three-tower directional-antenna array illustrated in Figure 8 are substituted in equation 5 the horizontal rms field intensity becomes

$$E_{\text{rms}} = \sqrt{\begin{aligned}&168^2+115^2+115^2\\ &+2(168)(115)\ \cos(0-296)\times J_0(3.07)\\ &+2(168)(115)\ \cos(0-218)\times J_0(1.745)\\ &+2(115)(115)\ \cos(218-296)\times J_0(3.37)\end{aligned}}$$

$$= \sqrt{\begin{aligned}&168^2+115^2+115^2\\ &+2(168)(115)(0.4384)(-0.2829)\\ &+2(168)(115)(-0.7880)(0.3719)\\ &+2(115)(115)(0.2079)(-0.3587)\end{aligned}}$$
$$=191$$

This shows an error of approximately one half of one per cent in the value 190 measured by the planimeter on the machine.

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Mobile Capacitor Units for Emergency Loading of Transformers in Open Delta

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Synopsis: Loading of closed delta banks of substation transformers to a point approaching safe thermal limit results in severe overloading of two transformers as well as decided unbalance in voltage and current when operated in open delta as a result of the failure of one transformer. By the use of mobile capacitor units with capacitors arranged in two unequal groups, the load on two transformers in open delta is reduced, regulation improved, voltage and current are balanced within operating limits, and with the aid of fans, the top oil temperatures—and consequently, the copper temperatures—are kept within safe limits. This permits proper loading of transformers in closed delta, a relatively short interruption to service or reduction in load upon failure of one transformer in a bank, and a reduction in number of transformers required for emergency use.

To maintain satisfactory service on a power system, mobile equipment such as railway-car-mounted substations[1] and highway trailer portable transformers have been found very effective. These portable transformers were designed to be used in case of the failure of more than one transformer in a bank. To maintain service in event of failure of a single transformer in a delta-delta bank, transformers of sufficient capacity were used to permit open delta operation without materially damaging the insulation from overheating during the repair of the damaged unit. This method should no longer be used, for it is now imperative that substation transformers be loaded to a point approaching the safe thermal limit. With transformers so fully loaded, the failure of one transformer in a bank will necessitate an interruption to service, or reduction in load for sufficient time to permit moving and installing additional transformer capacity, since operation in open delta would load the two transformers to 173 per cent of the closed delta loading. Operating at this overload would result in severe overheating of the two transformers and a very pronounced unbalance in voltage and current. Figure 1 shows calculated regulation of a typical bank of transformers in open delta with the two transformers operating at 173 per cent of rating. In addition to time and effort required to move and install emergency transformer capacity, this method requires a considerable stock of transformers of assorted sizes, voltages, and impedances on a system having a number of primary voltages. For example, the primary voltage of substation transformers on the system with which the authors are associated includes: 100 kv, 44 kv, 33 kv, 22 kv, 13.2 kv, 6.6 kv, and 2,400 volts.

An alternate method consists of open delta operation with the use of mobile capacitors and fans. As the power factor of load on substation transformers of the system on which these capacitors are used is ordinarily between 70 per cent and 80 per cent, the regulation of transformers operating at approximately 173 per cent of normal load in open delta is large and badly unbalanced. This excessive regulation and unbalance is corrected by connecting capacitors across the phase lagging the open delta and across the open delta. Figure 2A shows the calculated regulation of a bank of two 750-kva, 35-degree, 44,000–2,400-volt transformers both with and without capacitors and

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with a maximum load of 3,125 kva, the power factor being 74 per cent. Figure 2B shows transformer currents in each case. Capacitance of 1,080 kva was used across the phase lagging the open delta and 540 kva across the open delta. In making these calculations it was assumed for all loads that the load currents were equal in each phase and 120 degrees apart. The voltages on the source side of the transformers were assumed to be equal for each phase and 120 degrees apart. The power factor of 74 per cent was taken with respect to the balanced primary voltages. The per cent voltages shown on Figure 2A are the calculated values on the load side of the transformer bank. The curves of voltage are therefore somewhat distorted from what they would be in an actual application, because the primary and secondary voltages both would be somewhat unbalanced. In this application, however, when using capacitors, the voltage on both the primary and secondary windings of the transformer bank will be balanced at a load of 2,500 kva. This is shown for the load voltages on the curves of Figure 2A.

The loading of transformers in open delta with capacitance properly applied may be illustrated by the following example:

Assume a safe loading of three transformers to be 300 kva, the power factor, 75 per cent, and the kilowatts, 225. The load on each transformer, 100 kva, 75 kw.

In open delta, the loading of each transformer will be 173 kva, less a small decrease in output due to regulation.

If capacitance should be added to bring the power factor of each transformer to 100 per cent, the loading of each would be 112.5 kva, plus the capacitor losses, which are small.

Should capacitance be added to bring the power factor of the transformer which is connected across the phase lagging the open delta to 90 per cent, the load on each transformer will average approximately 125 kva, plus capacitor losses. This is still a satisfactory condition, as the regulation will be good, and transformers can be cooled with fans.

Method of Connecting Capacitance

Tests were made to determine the most effective application of capacitance to the phases of an open delta bank with loads of various power factors. A capacitance ratio of two parts across the phase lagging the open delta to one part across the open delta gave satisfactory results. This connection is shown schematically in Figures 4, 5, and 6, and is used in all mobile units. Thirty-six 15-kva capacitors rated 600 volts with 2,400-volt insulation to case were installed in each mobile unit. The groups of 12 and 24 capacitors were arranged for connection in parallel for 600-volt operation and four series-parallel for 2,400-volt operation. A mobile unit of this size was determined upon as a single unit is applicable to banks with loads as small as 300 kva and to banks with loads as large as approximately 1,500 kva, there being some 400 banks within these limits on the system on which these mobile capacitors are used. As can be seen in Figure 3, a unit of this capacity is comparatively small and can be handled easily.

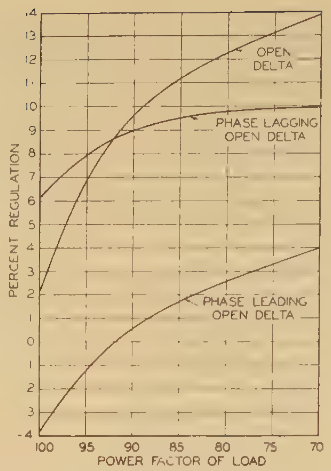


Figure 1. Calculated phase regulation of open-delta bank with transformers loaded to 173 per cent of rating

Transformer rating:
300 kva, 35 degrees centigrade rise
Voltage, 40,000/575

Regulation:
100 per cent power factor—0.94 per cent
90 per cent power factor—3.70 per cent
80 per cent power factor—4.65 per cent

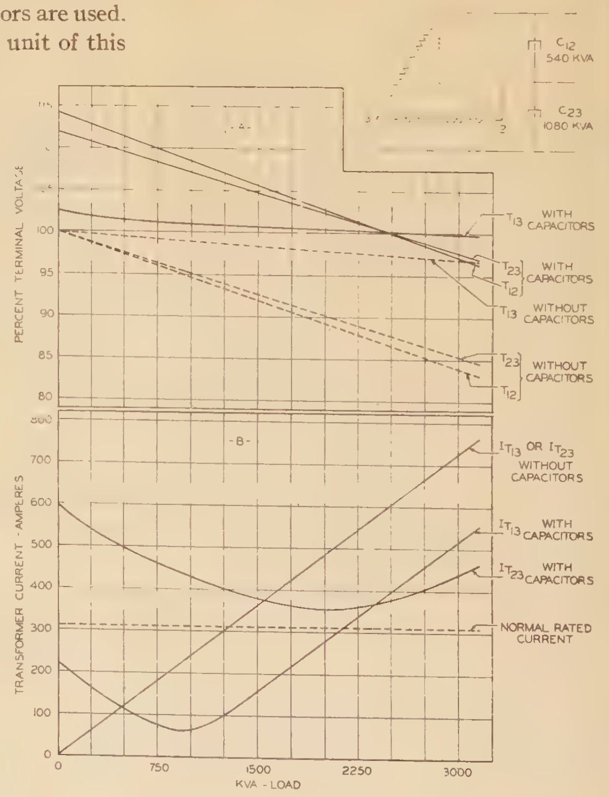


Figure 2. Calculated performance of transformers in open delta with and without capacitors

Transformer rating:
750 kva, 35 degrees centigrade rise
Voltage: 44,000—2,400

Regulation:
100 per cent power factor—0.94 per cent
90 per cent power factor—3.60 per cent
80 per cent power factor—4.75 per cent
74 per cent power factor—5.00 per cent

Power factor of load:
74 per cent

Per cent impedance:
0.9+j6.5 on 750 kva

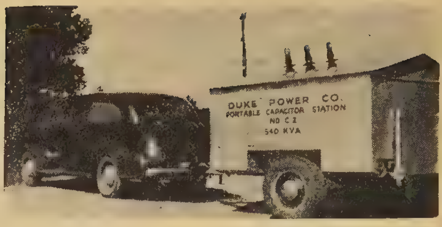
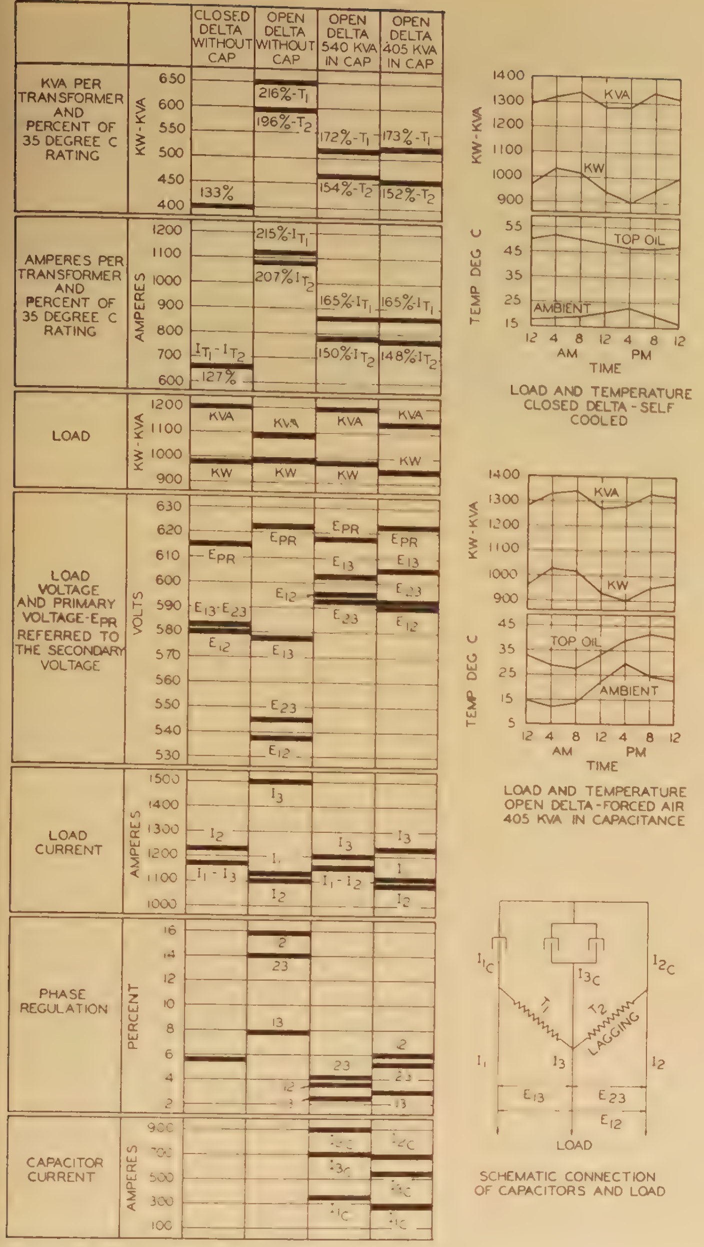


Figure 3. Mobile capacitor unit attached to towing vehicle

Tests and Operating Results

Data were obtained from capacitors in use as well as in tests in which one transformer was disconnected to give open delta connection. Figure 4 shows test on bank of 300 kva, 35-degree rise, 44,000/575-volt transformers with a peak load of over 1,300 kva and with complete readings taken with loads between 1,100 and 1,200 kva. Temperature curves show transformer temperatures while operating in closed delta, self-cooled, and in open delta, fan-cooled, with capacitors. One set of readings was taken with 25 per cent of the capacitors disconnected, but with the same ratio of 2 to 1 applied to the phase lagging the open delta and across the open delta. This test, as well as data in Figures 5 and 6, illustrates the wide range permissible in amount of capacitance used. Figure 5 shows test of bank



LOAD AND TEMPERATURE CLOSED DELTA - SELF COOLED

LOAD AND TEMPERATURE OPEN DELTA - FORCED AIR 405 KVA IN CAPACITANCE

SCHEMATIC CONNECTION OF CAPACITORS AND LOAD

Figure 4. Performance of transformer bank in closed delta and in open delta with and without capacitors

Transformer rating:
300 kva, 35 degrees centigrade rise
Voltage: 40,000/575

Regulation:
100 per cent power factor—0.94 per cent
90 per cent power factor—3.70 per cent
80 per cent power factor—4.65 per cent

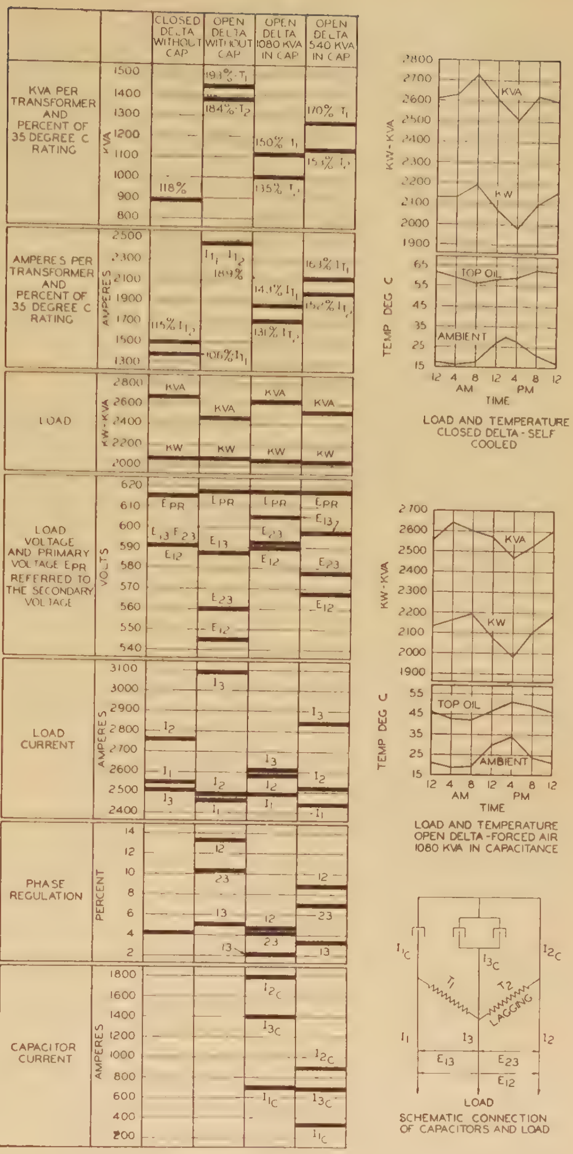
of 750 kva, 35 degrees centigrade, 44,000/575-volt transformers. In this case, capacitance of 1,080 kva was used for one set of readings and 540 kva for another, in each case the ratio of 2 to 1 across the lagging phase and open delta being retained. Figure 6 shows results with one 250-kva transformer of a bank of three out of service, a load of over 600 kw, with a normal power factor of approximately 80 per cent being carried by the remaining two transformers with a 540-kva capacitor unit. As in Figures 4 and 5, additional readings were taken with various amounts of capacitance.

The preceding figures illustrate the flexibility of the mobile capacitor units, for they are applicable to all open delta banks, regardless of primary voltage or impedance providing the secondary voltage is common to one of the two voltages for which these units are arranged. Provision is made to permit a portion of the 540 kva in each mobile unit to be disconnected quickly, still retaining the 2 to 1 ratio, if less capacitance is required for smaller loads, and, as shown in Figure 7, two or more units may be operated in parallel to take care of larger loads.

Description of Mobile Unit

As the total weight of the unit is only 4,600 pounds, it can be handled easily by car or light truck. Electric brakes are provided with brake control on a flexible cable, so each capacitor unit can be towed by any one of a number of vehicles.

The enclosing housing is provided with doors on each side, one side providing access to a 50,000-kva automatic oil circuit breaker which controls the capacitor circuit. The door on the opposite side opens to a terminal board by means of which the capacitors may be grouped for either 600- or 2,400-volt operation. One multitap current transformer on each of the two groups of capacitors provides current indication and tripping energy.



LOAD AND TEMPERATURE CLOSED DELTA - SELF COOLED

LOAD AND TEMPERATURE OPEN DELTA - FORCED AIR 1080 KVA IN CAPACITANCE

SCHEMATIC CONNECTION OF CAPACITORS AND LOAD

Figure 5. Performance of transformer bank in closed delta and in open delta with and without capacitors

Transformer rating:
750 kva, 35 degrees centigrade rise
Voltage: 44,000/600

Regulation:
100 per cent power factor—0.81 per cent
90 per cent power factor—2.73 per cent
80 per cent power factor—3.39 per cent

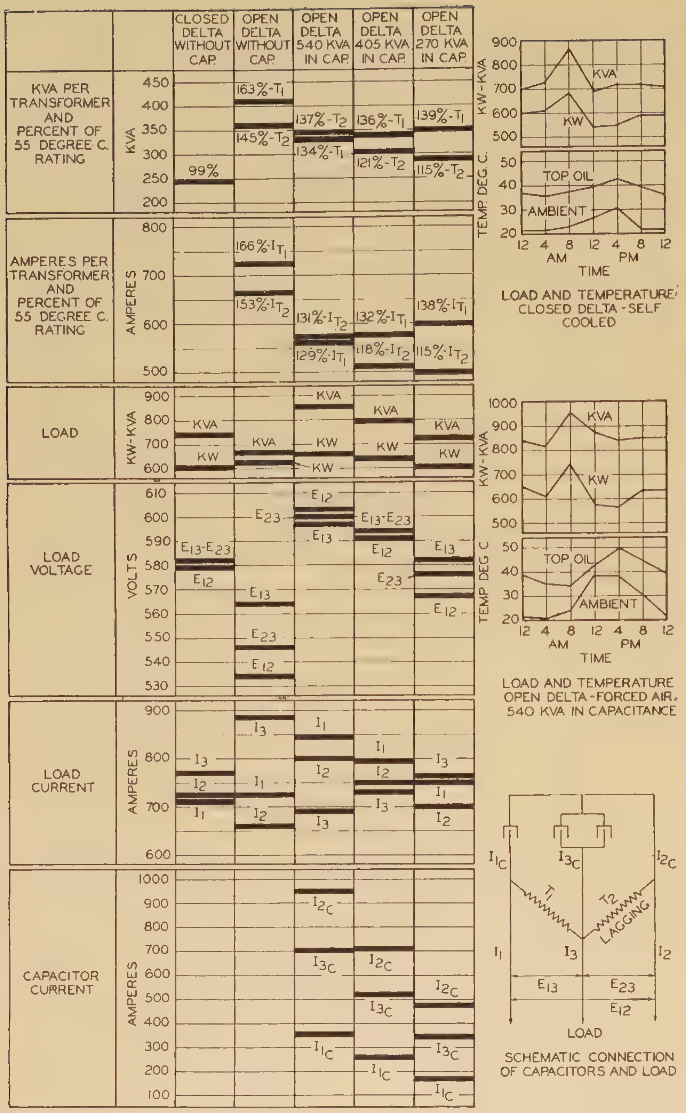


Figure 6 (left). Performance of open-delta transformer bank with and without capacitors

Transformer rating: 250 kva, 55 degrees centigrade rise
Voltage: 13,200/575

Regulation:
100 per cent power factor—1.2 per cent
80 per cent power factor—4.05 per cent

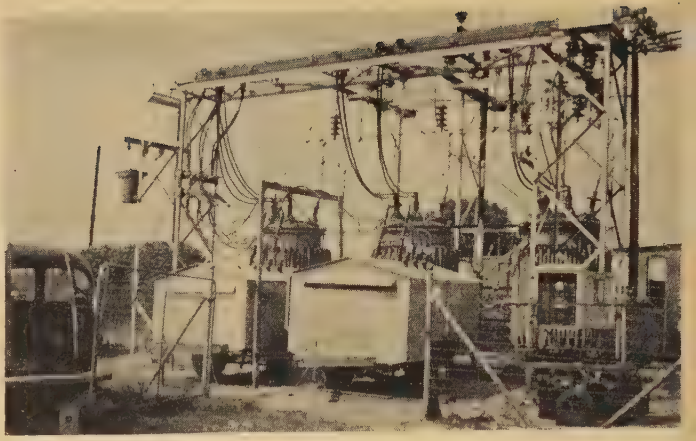


Figure 7. Test setup with two mobile capacitor units connected in parallel

Ventilation of the capacitors and breaker is provided by openings in the bottom and at each side of the enclosing housing. Natural circulation of air was found insufficient to maintain capacitor temperatures within desired limits so a 12-inch exhaust fan was installed in one side of each mobile unit. This provided ample circulation for properly cooling the equipment.

The entrance bushings are stenciled, "Common," "High," and "Low," referring to the transformer bus; the phase to which the two transformers connect being the "Common," the phase having the higher voltage as referred to the "Common" connecting to the "High" terminal, and the remaining phase to the "Low" terminal. With this marking correct connection of the portable unit to the transformer bus can be made quickly. Voltage readings are not required for correct connection if phase rotation is known

The portable fans can usually be transported in the towing vehicle. Two fans per transformer will ordinarily cool a transformer 500 kva or smaller, and three or more are used on a larger transformer.

Conclusion

By locating portable capacitor units at strategic points on a power system, permissible loading of closed delta banks may approach the safe thermal limit and still be operated in open delta in emergency. The mobility, light weight, and simplicity of connection, permit rapid installation of the portable capacitor units. As the mobile units are applicable to all transformer banks having secondary voltages common to the capacitors, the number of transformers held for emergency use may be reduced. This is particularly true on systems having a multiplicity of primary voltages.

Reference

1. A 100,000-VOLT PORTABLE SUBSTATION, Charles I. Burkholder and Nicholas Stahl. AIEE TRANSACTIONS, volume 34, 1915, pages 279–92.

Equivalent Ambient Temperatures for Loading Transformers

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MEMBER AIEE

Synopsis: Safe overloads for transformers can be determined by a modification of the American Standards Association rule for overloading to permit the use of an equivalent ambient temperature, which will result in the same loss of life as for continous operation at 30 degrees centigrade ambient and full load.

Based on the rule that aging of insulation doubles for each eight degrees centigrade increase in temperature, methods are developed for calculating the equivalent ambient temperature based on United States Government Weather-Bureau records. The equivalent ambient temperature for each month is calculated as the sum of the greatest recorded monthly mean temperature plus a daily correction factor based on the greatest recorded daily range, plus a monthly correction factor based on the greatest recorded monthly range. Monthly equivalent ambient temperatures so obtained can be used in combination with load curves to determine the permissible safe load for a transformer for a given locality.

USEFULNESS OF EQUIVALENT AMBIENT

TRANSFORMERS are given a name-plate rating in kilovolt-amperes which is the load they will carry under specified conditions. They may be safely overloaded beyond their name-plate rating under certain conditions, such as an ambient temperature below 30 degrees centigrade. In most locations ambient temperatures below 30 degrees centigrade exist for at least a portion of the time.

One method of conserving material is to utilize it to the best advantage so that greater output can be secured from a given quantity. Following these principles, transformers now in service can be used to carry loads greater than their name-plate rating, and new transformers with smaller name-plate ratings than the kilovolt-amperes to be carried can be secured for new applications.

PRINCIPLES GOVERNING EQUIVALENT AMBIENT

Under normal operating conditions a transformer ages because of the effect of temperature in deteriorating the insulation. The rate at which the life of the insulation is consumed increases as the operating temperature is increased. An increase in ambient temperature of the cooling medium results in a corresponding increase in the operating temperature of the transformer and corresponding increased rate of loss of life. Similarly, a decrease in the ambient temperature results in a decrease in the rate of loss of life.

Under actual operating conditions, the ambient temperature varies continuously. According to the American Standards Association Guides for Operation "oil-immersed self-cooled transformers may be overloaded continuously one per cent of rated kilovolt-amperes for each degree centigrade that the daily temperature of the cooling medium is below 30 degrees centigrade."

Accordingly, if the transformer is operating at constant load throughout the year, the maximum load will be based on the hottest day of the year. The use of an equivalent ambient temperature permits fuller utilization of transformer capacity, taking advantage of the fact that operation at low temperatures at lower rate of loss of life tends to compensate for operation for a time at high temperatures with faster rate of loss of life of insulation.

The equivalent ambient temperature is defined as the temperature which, if maintained continuously during the period of time under consideration, would result in the same aging of insulation as that occurring under the actual ambient temperatures. The equivalent ambient temperature may be used with the following modification of the American Standards Association rule for overloading transformers: "For low ambient temperatures the continuous kilovolt-ampere loading may be increased one per cent for each degree centigrade that the equivalent ambient temperature is below 30 degrees centigrade for self-cooled transformers."

The calculations for determining the equivalent ambient temperature are based on the laws governing the aging of transformer insulation. If the rate of aging were proportional to the temperature, the equivalent temperature would be equal to the average temperature. Actually, however, aging proceeds more rapidly at higher temperatures. The rate of aging doubles for each increase in temperature of five to ten degrees centigrade. Figure 1 is a curve which shows the rate of loss of life as a function of operating temperature, assuming the rate of aging doubles for each increase in temperature of eight degrees centigrade. Since the rate of aging shown in Figure 1 is relative, the ordinates (degrees centigrade) can represent actual degrees centigrade or degrees centigrade above any assumed base temperature.

Where the ambient temperature changes slowly, it has the effect of increasing the operating temperature of the transformer a corresponding amount. For example, if the ambient temperature is 40 degrees centigrade instead of 30 degrees centigrade, the operating temperature of the transformer will be ten degrees higher.

The method of calculating the equivalent ambient temperature is illustrated by Figure 2. Curve *A* is the recorded ambient temperature plotted versus time. Using curve *A* as a base and the relation between temperature and rate of loss of life in Figure 1, curve *B* is plotted, representing the rate of loss of life in per cent of the rate of loss of life at ten degrees centigrade corresponding to points on curve *A*. The degrees centigrade of each point of curve *A* above the assumed base temperature of 10 degrees centigrade are used to find the relative rate of aging for that point from Figure 1, for plotting on curve *B* of Figure 2. The area under curve *B* represents the total loss of life, and the average value of the ordinates of curve *B* is the average rate of loss of life for the period considered. The equivalent ambient temperature is the sum of the base temperature of ten degrees centigrade plus the temperature from Figure 1 corresponding to the average loss of life in Figure 2, since, if maintained constantly, this temperature would result in the same total loss of life as results from curve *A*.

Integration in exactly this manner could be used for determination of the equivalent temperature.

However, when using available data, the problem is considerably simplified if the year is considered on a monthly basis and the characteristics of a typical day for each month are determined. The monthly equivalent ambient temperatures are obtained by combining the daily equivalent ambient temperatures for the days of the month. In turn, the

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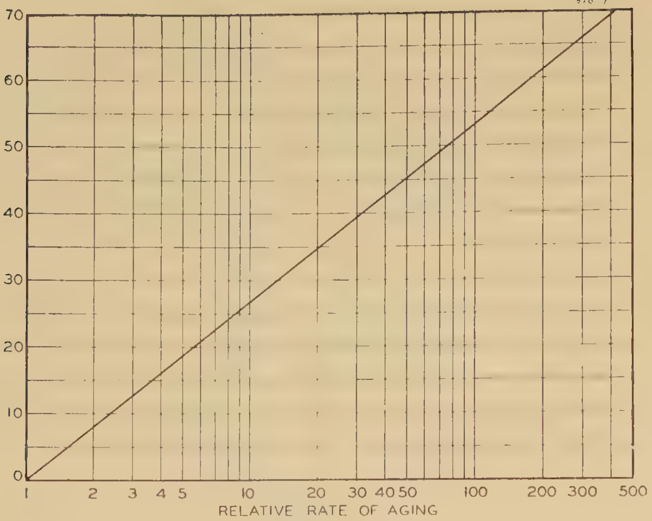
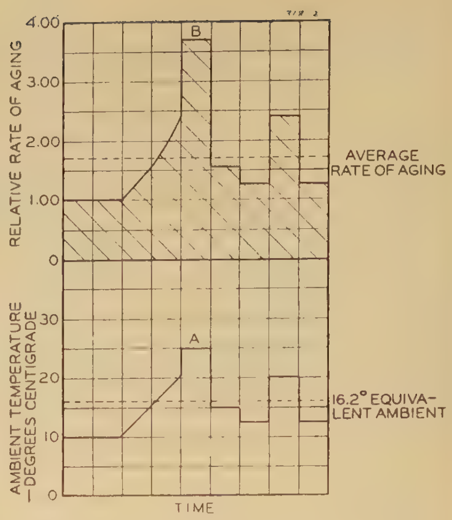


Figure 1 (left). Curve showing how rate of aging varies with the temperature according to the eight degrees centigrade rule

Figure 2 (right). Curves illustrating aging calculations



annual equivalent ambient temperature is obtained by combining the monthly equivalent ambient temperatures for the months of the year.

General Curves

Using the principle outlined in connection with Figures 1 and 2, it is possible to calculate the equivalent ambient for temperature variations with time for any shape of wave. A convenient manner of expressing the data so obtained is as a correction to be added to the average value of the wave or as a different correction to be subtracted from the maximum value of the wave. Curves showing the corrections to be added to the average value to obtain the equivalent temperature of a sine wave and of a wave in which the temperature varies as a straight line function of the time are shown in Figures 3 and 4. Figure 5 shows similar corrections to be subtracted from the maximum value of square top waves. These corrections in each case were obtained by calculating the equivalent temperature in the same manner as the equivalent temperature of Figure 2 was obtained.

Daily Temperatures

The records of the weather bureau for the location considered can be used to determine average daily temperatures and the variation during the day of the hourly temperature.

Transformers do not respond immediately to changes in ambient temperature because of the thermal capacity of their copper, iron, and oil. The effect of the thermal capacity of a transformer is twofold. It tends to smooth out and average the effect of rapid changes in temperature. In addition, it decreases the effect of the change so that a change of ten degrees in ambient during a 24-hour period, for example, will result in only an eight-degree change in the hot-spot temperature of the transformer. The mathematical relations governing this effect are derived in Appendix I where it is shown that the variation in temperature of a typical transformer caused by sine-wave variation in the ambient temperature during one day is 80 per cent of the ambient-temperature variation. The effect of the higher harmonics is smoothed out and decreased more than the temperature variation because of the fundamental frequency of one cycle per day.

Figure 6 shows typical daily variation in temperatures taken from reports of the United States Weather Bureau. This curve is of the general form of a sine wave with superimposed higher frequency harmonics of smaller magnitude.

The daily range in temperature may vary from a few degrees to as much as 50 degrees Fahrenheit or more. An examination of weather-bureau records of the range in temperatures indicates that the greater ranges may occur in connection with the highest temperature of the month or with the lowest or intermediate temperatures. Consequently, the assumption is made that every day has the same daily range in temperature. It will be noted that curve *A* of Figures 3 and 4 gives practically the same results as taking 80 per cent of the range and applying it to curve *B*. Consequently, for correcting for daily range the correction from curve *A* corresponding to the daily range may be added to the average temperature. The value of daily range to be chosen depends upon the conservatism desired. In order to insure conservative values of ambient temperature, the use of the greatest recorded daily range in temperature is recommended.

(The American Standards Association rule takes into account daily variations, and strictly speaking no correction would be made for them. However, in the interest of greater exactness, it seems logical to make this correction in order to obtain a constant base for the life of insulation instead of a varying base depending upon the locality.)

Monthly Temperatures

If the mean daily temperatures are averaged over a period of years, the average daily temperatures will vary with time in accordance with a sine wave. Figure 7 shows values for Milwaukee over a period of 55 years. The average maximum, the average mean, and the average minimum daily temperatures vary as sine waves with superimposed higher harmonics of smaller magnitude.

On the average the temperature during an individual month will follow a portion of a sine wave. Since there are 12 months

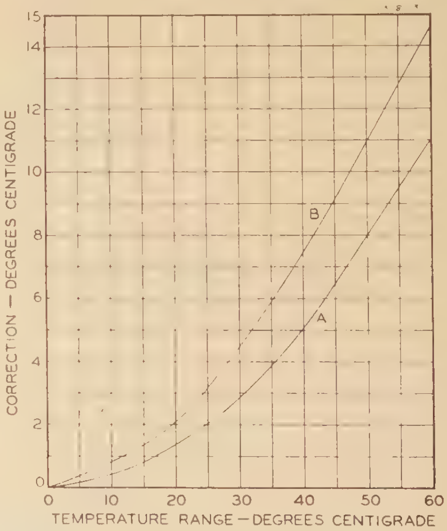


Figure 3. Correction curves—degrees centigrade

Curve *A*—Correction to be added to average value of straight-line variation of temperature to obtain equivalent temperature

Curve *B*—Correction to be added to average value of sine wave of temperature to obtain equivalent temperature

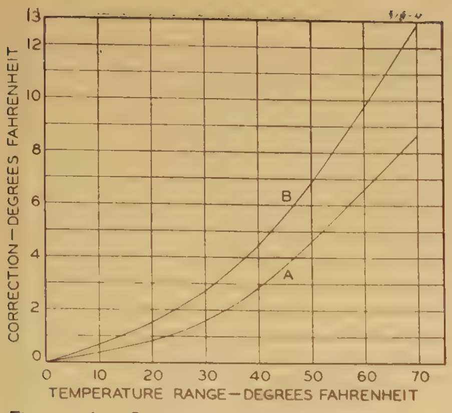


Figure 4. Correction curves—degrees Fahrenheit

Curve *A*—Correction to be added to average value of straight-line variation of temperature to obtain equivalent temperature

Curve *B*—Correction to be added to average value of sine wave of temperature to obtain equivalent temperature

in a year, and the period of yearly variation of temperature is 12 months, each month will extend over $^1/_{12}$ of 360 degrees, or 30 degrees. Since the curvature of each part of a sine wave is different, the actual shape of the ideal monthly variation will be different for each period of the year. If a straight line is drawn through the maximum value for the month and the average value for the month, a very close approximation will be obtained for periods of the order of one month. The portion of the curve which has the greatest effect on the aging is the portion above the average value, and it will be apparent by inspection of the straight lines *A*, *B*, and *C* of Figure 8 that very little error is introduced by assuming a straight-line variation of temperature with time during a month, if the straight line passes through the maximum value of temperature and the mean value of temperature located at the halfway point of the month. Figure 9 shows typical values of the variation of daily temperature during the month. The mean temperatures plotted are arranged in their order of magnitude with the hottest day plotted at the left and progressing to the coldest day at the right. These curves are in accordance with the ideal variation in that a straight line drawn through the maximum daily mean temperature for the month and the monthly mean temperature results in a very good approximation of the monthly equivalent. Consequently, curve *A* of Figures 3 and 4 can be used to obtain the correction for variation of daily mean temperatures during the month. The range in temperatures to be used depends upon the conservatism desired. In order to insure conservative values of ambient temperature, the use of the greatest monthly range of daily mean temperatures ever recorded for that month is recommended. This figure is not always readily available. In such cases one of the other approximations given in Appendix III can be used.

Figure 10 shows a nontypical variation of temperatures during a month in which the temperatures vary considerably from a straight line. However, even here the straight line drawn through the maximum and the mean temperatures represents an equivalent temperature that is not greatly in error, and such a wave shape is certain to have an average value that is lower than the greatest average value ever recorded for the month, because of the effect of the low values in reducing the average. This is illustrated by the figure where the actual mean was 20 degrees Fahrenheit for the month and greatest mean recorded for that month is 34 degrees Fahrenheit.

Reports of the weather bureau show the mean temperature for each month over a large number of years. By selecting the greatest mean temperature which has been recorded for that month, a value of average temperature is obtained which is not likely to be exceeded in the future years or in any case by more than a small amount. Using these values, the equivalent monthly ambient is equal to the greatest mean temperature ever recorded for the month, plus the daily correction corresponding to the greatest daily range ever recorded for the month from curve *A* of Figures 3 or 4 plus the monthly correction corresponding to the greatest monthly range in daily mean temperature ever recorded for the month from curve *A* of Figures 3 or 4.

After equivalent temperatures for each of the 12 months of the year have been calculated, the equivalent temperature for any period of time involving two or more months can be determined readily by use of Figure 5. By successively combining months, the equivalent temperature for the complete year may be obtained with accuracy, without making assumptions as to the variation of the average monthly temperatures throughout the year.

Source of Data

The most convenient source of temperature data to be used in calculating the equivalent ambient is the annual and monthly meteorological summaries of the location concerned which can be obtained in each case from the local weather bureau. A list is given in Appendix II of the cities which have United States Weather Bureaus which issue meteorological summaries. In addition, weather bureaus can furnish certain specific data on request. An example of such data is the greatest daily range in temperature which has ever occurred during a given month since an early date.

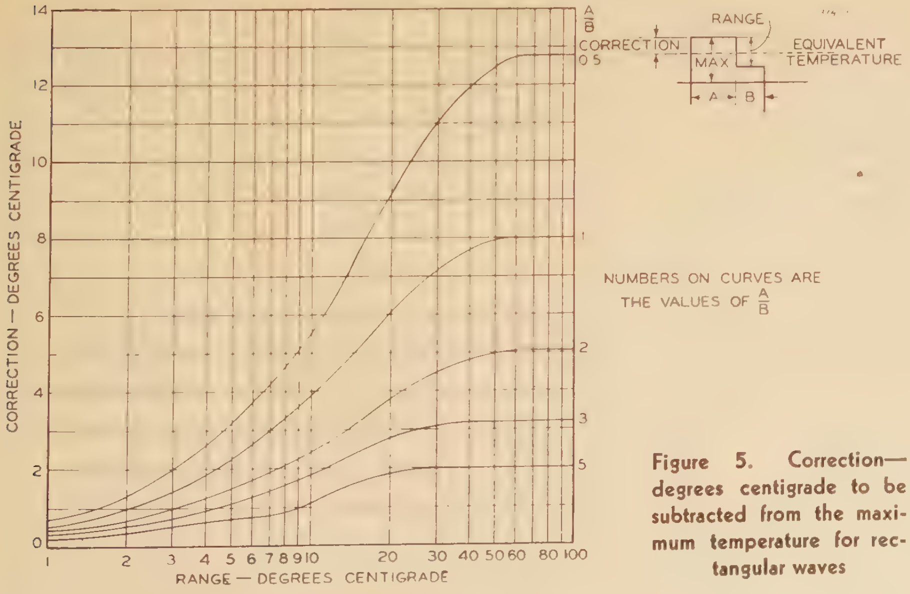


Figure 5. Correction—degrees centigrade to be subtracted from the maximum temperature for rectangular waves

Recommended Rules for Calculating Equivalent Ambient

1. The daily correction is read from curve *A*, Figure 3 or 4, corresponding to the value of the greatest recorded daily range in temperatures for the month.

2. The monthly correction is read from curve *A*, Figure 3 or 4, corresponding to the greatest recorded range of daily mean temperatures for the month. (See appendix for approximations.)

3. The equivalent monthly temperature equals the maximum mean temperature for the month considered, for any year plus the

Table I. Typical Calculation of Equivalent Ambient Temperatures

Month		Jan	Feb	Mar	Apr	May	June	July	Aug	Sept	Oct	Nov	Dec
Greatest daily range		36	44	46	40	39	34	29	30	42	33	43	43
Greatest monthly range in daily temperature		49	51	50	44	39	34	30	27	34	40	51	46
Greatest monthly mean		46.2	42.2	52.5	59.2	67.9	78	80.6	79.2	75.4	62.8	50.9	44.6
Daily correction		2.3	3.5	3.8	2.8	2.7	2.0	1.5	1.6	3.1	1.9	3.3	3.3
Monthly correction		4.4	4.8	4.6	3.5	2.7	2.0	1.6	1.3	2.0	2.8	4.8	3.8
Equivalent ambient (degrees Fahrenheit)		52.9	50.5	60.9	65.5	73.3	82.0	83.7	82.1	80.5	67.5	59.0	51.7
Equivalent ambient (degrees centigrade)		12	10	16	19	23	28	29	28	27	20	15	11
		←11.1→		←17.6→		←25.8→		←28.5→		←24.1→		←13.2→	
		←14.9→				←27.2→				←20→			
		←22.7→								←20→			
		←21.9→											
		←11.1→		←19.6→			←28.3→			←20→			
	C	←16.8→					←28.3→			←20→			
Permissible per cent overload *C*		←13.2→					←1.7→			←10→			
Equivalent ambient—*D*		←15.9→					←28.3→			24.1		←15.9	
Permissible per cent overload *D*		←14.1→					←1.7→			5.9		←14.1	

daily correction from rule 1 plus the monthly correction from rule 2.

4. The equivalent temperatures for periods of two or more months may be obtained by combining equivalent monthly temperatures using the curves of Figure 5 and subtracting the correction of Figure 5 from the higher equivalent ambient monthly temperature.

APPLICATION OF RULES FOR CALCULATION OF EQUIVALENT AMBIENT

Table I shows how these rules may be applied to calculating equivalent ambient temperature by their application to the Philadelphia district. The data were obtained from the local weather bureau. The first three lines of Table I contain data obtained from the weather bureau in Philadelphia. The fourth and fifth lines are the daily and monthly corrections from Figure 4 corresponding to the temperature ranges in lines 1 and 2, respectively. The equivalent ambient (degrees Fahrenheit) is the sum of the preceding three lines. The equivalent ambient in degrees Fahrenheit is converted into degrees centigrade. In the following lines, the months are combined into longer periods, using the curves of Figure 5. The period considered is represented by the arrows. The equivalent yearly ambient is 21.9 degrees centigrade According to the modification of the American Standards Association rule, a transformer can carry 30−21.9 or 8.1 per cent additional load continuously in this locality; that is, a load of 108 per cent of the name-plate rating. This is shown by curve *C* of Figure 11. Curves *A* and *B* are determined similarly.

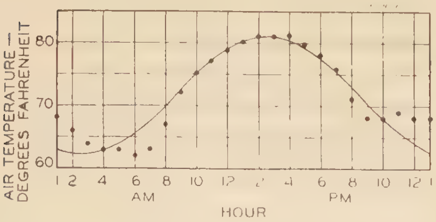


Figure 6. Typical variation of ambient temperature during 24-hour period

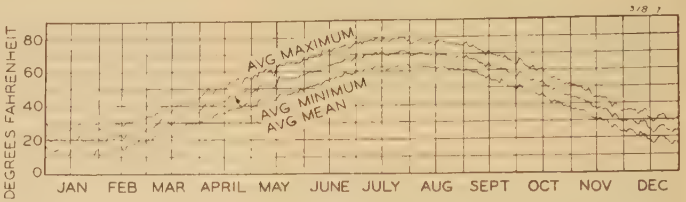


Figure 7. Typical variation of average daily temperatures over a period of 55 years

APPLICATION OF EQUIVALENT AMBIENTS TO LOAD CURVES

Any yearly load curve can be plotted in per cent of the maximum load as shown by Figure 12, curve *B*. By plotting the permissible load for the same periods of time as the load blocks on the same scale, an indication will be obtained as to which curve best fits the load scale. In some cases a recombination of curves will allow the load curve to be approached more closely. It should be noted that only one of the safe load curves may be used. That is, any curve has the same loss of life as any other curve, but it is not permissible to use part of one curve for a portion of the year and part of another curve for another portion of the year. It is, of course, permissible to divide the permissible load curves into blocks of uneven periods of time, as is shown by curve *C* of Figure 12. This is accomplished by the use of the curves of Figure 5 in connection with the equivalent monthly ambient temperatures.

Example. Assume the maximum kilovolt-amperes of load curve *B* of Figure 12 is 8,000 at 80 per cent daily

Table II. Equivalent Ambient Temperatures (Degrees Centigrade) for Various Localities

	Annual	Jan	Feb	Mar	Apr	May	June	July	Aug	Sept	Oct	Nov	Dec
Atlanta	25	16	17	20	23	27	30	30	30	31	25	20	18
Bismarck	21	3	8	14	17	23	27	32	28	25	19	11	7
Boston	20	8	7	11	15	22	27	28	26	24	20	13	11
Chattanooga	24	16	17	20	22	26	30	30	30	28	25	20	15
Chicago	21	10	10	13	18	23	28	28	27	25	19	14	11
Corpus Christi	28	22	22	25	27	28	30	31	31	30	29	25	24
Dallas	28	25	20	23	26	28	32	33	33	31	26	21	18
Denver	23	12	13	15	19	23	27	30	31	26	20	15	14
Detroit	20	6	8	11	17	23	27	28	27	25	18	13	8
El Paso	26	15	15	20	24	28	32	32	31	30	25	20	16
Helena	20	10	8	11	16	20	25	29	27	22	17	11	10
Kansas City	25	11	13	20	21	26	32	33	33	28	22	18	13
Los Angeles	24	20	20	22	22	23	25	27	27	27	25	22	20
Louisville	24	16	13	18	22	26	28	31	30	29	22	20	17
Miami	28	27	25	26	28	28	29	30	30	29	28	27	27
Milwaukee	20	6	6	10	16	21	27	28	26	23	18	13	7
Minneapolis	21	2	3	12	18	24	28	30	29	24	18	9	6
New Orleans	27	23	23	24	25	28	31	31	31	30	29	23	22
New York	21	11	9	14	18	23	28	29	27	26	20	14	11
Oklahoma City	25	17	19	21	23	26	32	32	33	31	26	20	13
Philadelphia	22	12	10	16	19	23	28	29	28	27	20	15	11
Phoenix	31	18	20	23	28	32	36	39	38	35	30	22	18
Pittsburgh	23	12	12	15	20	25	27	30	29	28	21	17	12
St. Louis	25	14	13	20	22	26	32	33	32	27	22	20	15
Salt Lake City	22	6	9	13	18	24	29	32	31	26	19	11	8
San Francisco	19	14	15	18	19	20	21	20	20	22	20	18	14
Seattle	18	10	11	16	17	20	21	23	22	20	17	12	10
Washington, D. C.	23	12	12	18	21	26	28	31	30	28	21	16	12

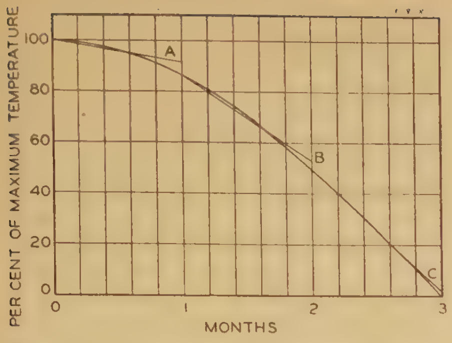


Figure 8. Comparison of assumed straight lines with ideal sine waves for variation during a monthly period throughout the year

load factor. The location is Philadelphia. The correction for load factor is six per cent according to the interim report on transformer overloading and its effect is considered separately. Curves *A* and *C* are calculated as shown on Table I. If the unit is loaded by curve *A*, it will be noted that curves *A* and *B* are closest from January to April. Consequently, the transformer kilovolt-amperes required will be

$$\frac{8,000}{1.08}=7,400 \text{ kva}$$

If curve *C* is used, the transformer kilovolt-amperes required is 8,000/1.13 = 7,070 kva. An examination of Table I shows that the equivalent ambient for November and December is lower than for the critical period, January to May. Consequently, greater overloads can be carried during the critical period if November and December are added to the block of the critical period to obtain curve *D* of Figure 12. If curve *D* is used, the transformer kilovolt-amperes required is

$$\frac{8,000}{1.14}=7,020 \text{ kva}$$

The ordinates of curve B' are equal to the ordinates of curve *B* multiplied by 8,000/7,020. A still further correction of six per cent can be made for the load factor so that the transformer kilovolt-amperes required =

$$\frac{7,020}{1.06}=6,620 \text{ kva}$$

That is, 6,620 transformer kilovolt-amperes is required to supply a maximum load of 8,000 kva, which varies during the year in accordance with load curve *B* with an 80 per cent daily load factor.

EQUIVALENT AMBIENT TEMPERATURES FOR VARIOUS LOCALITIES

Table II shows calculated values of monthly and annual equivalent ambient temperatures for various localities in the United States. These values can be used for calculating permissible overloads for transformers.

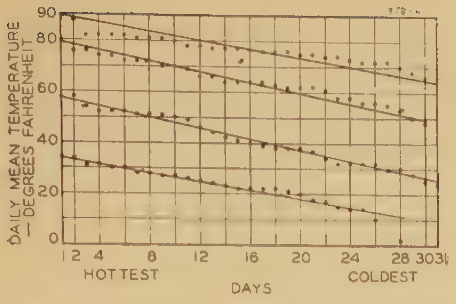


Figure 9. Typical variation of daily mean temperatures during a month arranged in the order of magnitude for four different months

The straight lines are drawn through the maximum and the mean temperature points

By reference to this table, it will be noted that there is much greater variation in winter monthly ambient temperatures among localities than there is variation in the annual ambient temperatures or summer ambient temperatures. For example, during January the variation is from two degrees centigrade for Minneapolis to 25 degrees centigrade for Dallas, compared to an annual variation of from 20 degrees centigrade for Boston to 31 degrees centigrade for Phoenix. During July the variation is only from 28 degrees centigrade for Boston to 39 degrees centigrade for Phoenix and 33 degrees centigrade for Dallas. By the eight-degree rule, the higher temperatures which occur have a much greater effect on the value of the equivalent ambient than the lower temperatures. The variation among localities of the highest temperatures is less than the variation among localities of the lowest temperatures. Consequently, the greatest variation among localities in safe loads will be during the winter months, not during the summer nor on an annual basis.

These facts, in combination with the example in Table I, illustrate the value of using monthly equivalent ambient temperatures in combination with a yearly load curve for obtaining maximum output from a given transformer.

ACCURACY OF METHOD

The greatest values ever recorded have been used for monthly mean temperatures, monthly range in temperatures, and daily range in temperatures. Typically shaped curves for the manner of variation of temperature with time have been used. The eight-degree rule has been assumed for the variation of the life of insulation. The life base used is that of a transformer operated continuously at full load and 30 degrees centigrade ambient.

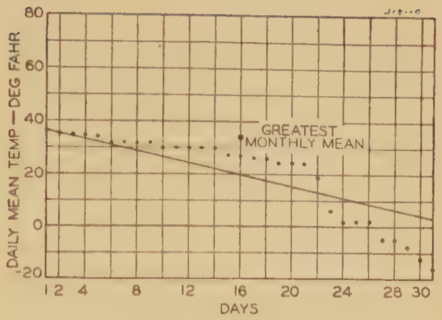


Figure 10. Nontypical variation of daily mean temperature during a month

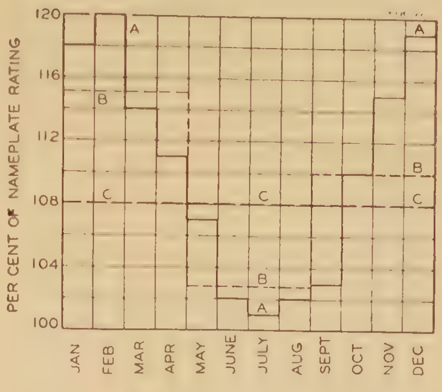


Figure 11. Safe overload curves for Philadelphia

Curve *A*—Monthly curve
Curve *B*—Curve in four month blocks
Curve *C*—Constant load curve

For most years the actual values of every one of the factors of mean temperature and ranges will be less than the values assumed. In addition, if one factor is low, it will tend to compensate for higher values of other factors. Consequently, the loss of life in nearly all years will be considerably less than the assumed value. The fact that low loss of life in one year will offset a high loss of life in another year provides an additional factor of safety. Such conservative relations are desirable, since, if the equivalent ambient is to be safe for any year, it must be safe for the worst year which is liable to occur.

It is, of course, possible to have an unusual combination of mean temperature, temperature ranges, and shape of variation curves such as to produce a higher loss of life in a month than the base value, but such an occurrence is extremely unlikely. However, should such a combination occur, its effect will be to exceed the base by only a small amount and very infrequently, and therefore it can be considered in the same class as an emergency overload.

The eight-degree rule has been found to be approximately correct by several

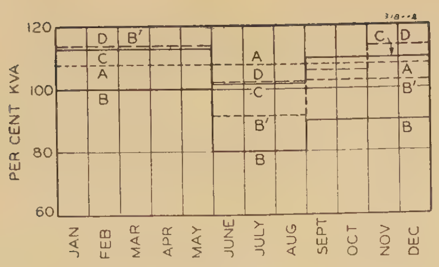


Figure 12. Determination of permissible overload for a given load curve

Curve *A*—Permissible constant load curve

Curve *B*—Actual load curve in per cent of maximum load

Curve *B*¹—Actual load curve in per cent of transformer kilovolt-amperes

Curve *C*—Permissible load curve

Curve *D*—Permissible load curve

independent investigators at various times, and indications are that a five-degree rule or a lower-temperature rule than eight degrees centigrade is unduly pessimistic for a transformer which has its insulation in good condition.

Conclusions

1. The use of equivalent ambient temperatures permits the load capacity of transformers to be utilized more fully, so that greater loads may be carried on present transformers, or so that smaller transformers may be secured for a given load.

2. The equivalent ambient temperature can be calculated from the records of the local weather bureau, as the monthly mean temperature plus a correction for daily range in temperature plus a correction for a monthly range in temperature.

3. Values of equivalent ambient temperatures which permit overloads to be carried with demonstrable safety can be obtained.

4. The rules given in the text permit the determination of the equivalent ambient temperatures and corresponding overloads with a limited amount of calculation.

5. The monthly equivalent ambient temperatures for each month of the year and combinations of them are more generally useful than the annual equivalent ambient for determining permissible overloads on transformers.

Appendix I. Effect of the Thermal Capacity of a Transformer in Modifying Daily Temperature Variation

In the portion of the heat circuit shown in Figure 13, heat is assumed to be flowing from point *A* to point *B* through the heat-resistive material (shown shaded) with a resistance *R* where *R* equals the temperature difference required to cause one watt of power to flow through the thermal circuit under steady conditions. Let the thermal capacity be *K*, where *K* is the watt-hours per degree centigrade thermal capacity of the body considered. Let *W* equal the watts heat flow from *A* to *B*, t=time and T_R= the temperature difference between *A* and *C*. In this circuit the energy flowing in time dt is equal to Wdt, and the heat absorbed is equal to the thermal capacity times the change in temperature or KdT_x. (T_x=temperature drop across *K*.) Since all the heat is absorbed, the heat flowing equals the heat absorbed or

$$Wdt=KdT_x$$

Solving for T_x,

$$T_x=\frac{1}{K}\int Wdt$$

The temperature drop across the resistance *R* is equal to *RW*. Therefore, the total temperature drop in the circuit is equal to

$$T=RW+\frac{1}{K}\int Wdt$$

The equation of an electric circuit containing resistance and capacitance only is given as follows:

$$e=rI+\frac{1}{C}\int Idt$$

By inspection, it is evident that *T* in the equation corresponds to *e*, *R* corresponds to *r*, W corresponds to *I*, and *K* corresponds to *C*. It is evident that thermal capacity bears the same relation to the heat circuit that the electrical capacity has in the electric circuit.

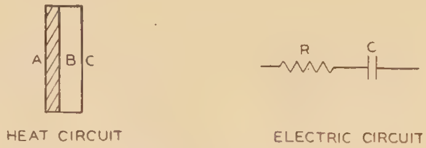


Figure 13. Heat circuit and analogous electric circuit

The reactance to heat flow=*X*, and, by analogy to the electric circuit, is equal to $1/2\pi fK$, where f=the frequency. For a typical transformer with a 45 degrees centigrade oil rise for continuous operation, a thermal time constant of three hours, and a watts loss of *W*, the resistance to heat flow in the circuit, $R=45/W$.

Normally, the ambient temperature of a transformer varies according to a daily cycle with a high point during the day and a minimum during the night, according to a sine law with sometimes superimposed higher-frequency variations (Figure 4). Consequently, f can be assumed to be=1/24 cycles per hour.

The thermal time constant is the time required to reach the ultimate temperature, if all heat were absorbed by the thermal capacity of the transformer. Consequently, for a typical transformer when the thermal time constant is 3, the value of

$$K=\frac{W\times 3}{45}=\frac{W}{15}$$

$$X=\frac{1}{2\pi fK}=\frac{1}{2\pi\times\frac{1}{24}\times\frac{W}{15}}=\frac{180}{\pi W}$$

$$\frac{X}{R}=\frac{180}{\pi W}\times\frac{W}{45}=1.27$$

The temperature rise of the transformer is equal to the thermal drop across *X*. Consequently, the proportion of the total temperature which appears as the temperature rise of the transformer is equal to the sine of the angle, the tangent of which=X/R=1.27. The sine of the angle =0.79. That is, for a typical transformer the variation in temperature of the transformer due to daily variation in the ambient temperature is 80 per cent of the ambient variation.

Appendix II. Locations of Government Weather Bureaus from Which Meteorological Summaries May Be Obtained

ALABAMA
Birmingham
Mobile
Montgomery

ARIZONA
Flagstaff
Phoenix
Yuma

ARKANSAS
Fort Smith
Little Rock

CALIFORNIA
Eureka
Fresno
Los Angeles
Redding
Sacramento
San Diego
San Francisco

COLORADO
Denver
Grand Junction
Pueblo

CONNECTICUT
Hartford

DELAWARE
Delaware Breakwater

DISTRICT OF COLUMBIA
Washington

FLORIDA
Apalachicola
Jacksonville
Key West
Miami
Tampa

GEORGIA
Atlanta
Augusta
Macon
Savannah

IDAHO
Boise
Pocatello

ILLINOIS
Chicago
Peoria
Springfield

INDIANA
Evansville
Fort Wayne
Indianapolis

IOWA
Charles City
Davenport
Des Moines
Dubuque
Sioux City

KANSAS
Concordia
Dodge City
Kansas City
Topeka
Wichita

KENTUCKY
Louisville
Pikeville

LOUISIANA
New Orleans
Shreveport

MAINE
Eastport
Portland

MASSACHUSETTS
Boston

MICHIGAN
Alpena
Detroit
Escanaba
Grand Rapids
Houghton
Marquette

MINNESOTA
Duluth
Minneapolis

MISSISSIPPI
Meridan
Vicksburg

MISSOURI
Columbia
Kansas City
St. Joseph
St. Louis
Springfield

MONTANA
Billings
Havre
Helena
Kalispell
Miles City

NEBRASKA
Lincoln
North Platte
Omaha
Valentine

NEVADA
Reno
Tonopah
Winnemucca

NEW HAMPSHIRE
Concord

NEW JERSEY
Atlantic City

NEW MEXICO
Albuquerque
Roswell

NEW YORK
Albany
Buffalo
Canton
New York
Rochester
Syracuse

NORTH CAROLINA
Asheville
Charlotte
Hatteras
Raleigh

NORTH DAKOTA
Bismarck
Devils Lake
Fargo
Williston

OHIO
Cincinnati
Cleveland
Columbus
Toledo

OKLAHOMA
Oklahoma City

OREGON
Portland
Roseburg

PENNSYLVANIA
Erie
Harrisburg
Philadelphia
Pittsburgh

SOUTH CAROLINA
Columbia
Charleston

SOUTH DAKOTA
Huron
Rapid City

TENNESSEE
Chattanooga
Knoxville
Nashville

TEXAS
Abilene
Amarillo
Austin
Brownsville
Corpus Christi
Del Rio
Dallas
El Paso
Fort Worth
Galveston
Houston
San Antonio

UTAH
Salt Lake City

VERMONT
Burlington

VIRGINIA
Lynchburg
Norfolk
Richmond

WASHINGTON
Seattle
Spokane
Walla Walla

WEST VIRGINIA
Columbia
Charleston

WISCONSIN
Green Bay
La Crosse
Madison
Milwaukee

WYOMING
Cheyenne
Lander
Sheridan
Yellowstone Park

Appendix III

In many cases local weather bureaus have compiled from their records values of the greatest range in daily mean temperature during each month in any one year. When this figure is not available, some other approximation of the monthly range may be used in its place. One convenient base for such an estimate is the greatest daily range ever recorded during the month. An examination of temperature records will show that the greatest range in monthly mean temperature is approximately the same as the greatest recorded daily range for the month. The correlation is not accidental. If the phenomena concerned are investigated, it will be apparent that large daily changes are due to changes in the wind direction from a cooler region to a warmer region or vice versa combined with the effect of sunshine and precipitation, both factors being reduced in effect by the thermal capacity of the earth. Monthly variations are due to the same causes, and a correlation between the variation of daily mean temperature during the month and the maximum variation of temperatures during the day would be expected. For calculation several alternatives are possible: 1. To assume that the monthly range of daily mean temperature is equal to the greatest daily range for that month. 2. More conservatively, the monthly range may be assumed to be equal to the greatest daily range ever recorded for that locality. Still another approximation for the monthly range may be based on the greatest daily mean temperature ever recorded in that month and the greatest monthly mean temperature ever recorded for that month. Twice the difference of these two values may be used as the value of the monthly variation.

It is obvious that the value of the equivalent ambient temperature obtained will be different, depending upon which one of these approximations is used. However, the difference will not be great, and the overloads obtained will be reasonably safe. The more conservative the figure used for the monthly variation, the less frequent will be overloads combined with ambient temperatures which must be classed as emergency conditions. The life of the transformer will be prolonged if emergency conditions occur with less frequency. The safest practice is, of course, to use the most conservative of the methods in cases where data for different methods are available.

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Wood-Pole 230-Kv Transmission Lines

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MEMBER AIEE

STEEL has been comparatively cheap in the United States and, until recently, available in quantity. Consequently the use of steel structures for supporting the higher-voltage transmission lines has been favored by transmission engineers. However, since conservation of critical materials has become necessary, it is essential that designs incorporating wood construction be considered for such applications. A contribution to efforts in this direction is provided by a review of the design used for single-circuit wood-pole 230-kv transmission lines recently constructed to supply power to an important industrial area.

Early in 1942 plans for this project, including steel-tower designs, had been completed, and materials were about to be ordered when restrictions regarding the use of steel became effective. Subsequently, the project was redesigned for wood-pole construction, and the entire project consisting of 158 miles of single-circuit 230-kv line was completed and placed in operation $5^1/_2$ months later.

The decision to use wood construction precipitated a hasty review of previous experience with such designs. It was found that wood-pole construction had been used in Sweden for 230-kv operation with satisfactory results. However, detailed information regarding these lines was not available. These lines are the longest wood-pole lines built in the United States for 230-kv operation and the only ones for which wood has been used for all structures and all types of structures.

Design Premises

Conventional designs for transmission lines operating at lower voltages were available, and many had been proved satisfactory over long periods of operation. The general form of the two-pole *H*-frame structure appeared acceptable, though it was found that the problem of design was increased greatly because of increased insulation and conductor-clearance requirements, greater mechanical loads caused by the increased conductor diameters, and correspondingly increased ice and transverse wind loads. It was intended that the lines should be designed to operate with a high degree of reliability and that high factors of safety should be employed in consideration of the importance of the connected load and the complete lack of previous experience with wood-pole construction for 230-kv operation. To attain high electrical reliability, advantage was taken of the insulating value of the wood supporting members in the paths between conductors and ground. Suspension insulator strings of 15 $5^3/_4$-inch units were provided, and these were supported through large metallic surfaces in order to distribute leakage currents. Experimental data regarding the minimum insulation required for 230-kv operation to prevent digestion and burning of crossarm material were not available. Operating records of lower-voltage wood-pole lines with proportionately less porcelain insulation indicate that the number of insulator units selected for these lines will assure satisfactory performance.

The impulse flashover voltage ($1^1/_2$x 40-microsecond negative wave) of a 15-unit insulator string is 1,345 kv, which is the flashover voltage of a rod gap of 76 inches. The minimum clearance of the conductor from the pole ground wire was maintained at 86 inches. This value of minimum clearance permitted a maximum conductor displacement of 37.5 degrees from vertical. Minimum clearance to crossarms and other wooden members was 76 inches. For determinations of maximum conductor displacement, a wind pressure of eight pounds per square foot on bare conductor was assumed.

Crossarm Design

The length of crossarm required to maintain clearance desired under these conditions was 50 feet, which length provided a conductor separation of 24 feet. Each crossarm consisted of 3- by 12-inch timbers, two 28-foot lengths and two 22-foot lengths, spliced at the center by means of a block 10 feet long and 12 by $7^1/_4$ inches in cross section. A lapped joint was designed using bolts and split-ring timber connectors between surfaces in contact. Blocks of similar dimensions except four feet in length, were installed between the 3- by 12-inch members at the ends of the crossarm, also by means of bolts and timber connectors. These blocks served to increase greatly the strength of the arm in the longitudinal direction.

Conductors

Hollow copper conductors of 500,000 circular mils cross section, 1.1 inches in diameter, were installed. The conductor loading assumed was one-half inch radial ice with an eight-pound wind at zero degrees Fahrenheit. The maximum tension under this loading condition is 9,000 pounds or 41.5 per cent of the ultimate strength. Two overhead ground wires, each consisting of three number 5 extra-high-strength copper-covered steel strands, were installed and located so as to provide 30-degree angles of protection for each conductor. The maximum tension of overhead ground conductors is 4,300 pounds or 36 per cent of the ultimate strength.

Structure Design

To meet National Electric Safety Code requirements for grade-*B* construction and to provide sufficient strength for spans of reasonable length, class-2 poles were specified and installed. Poles 65 feet in length were required in order to provide level ground spans of 520 feet and the desired final clearances of conductors to ground of 32 feet at 60 degrees Fahrenheit. The pole-market situation early in 1942, however, was such that 65-foot poles were not available in the necessary quantities, though relatively greater quantities of 60-foot class-2 western-red-cedar, southern-pine, and Douglas-fir poles were available for early shipment. Poles 60 feet in length were purchased and were made to serve the purposes of 65-foot poles by installing latticed steel-channel extensions on the tops of each of the poles of the two-pole structures.

The top of the steel-pole extension and the end of the crossarm were connected through a tension member, and the tops of the extensions were in turn tied together by means of one-half-inch Siemens-Martin strand. Formed channel bayonets were bolted to the steel-pole extensions to support the overhead ground wires.

The structure was strengthened by means of a *K*-brace assembly consisting of 3- by 10-inch timbers and 3- by 6-inch timbers, and an *X*-brace consisting of two

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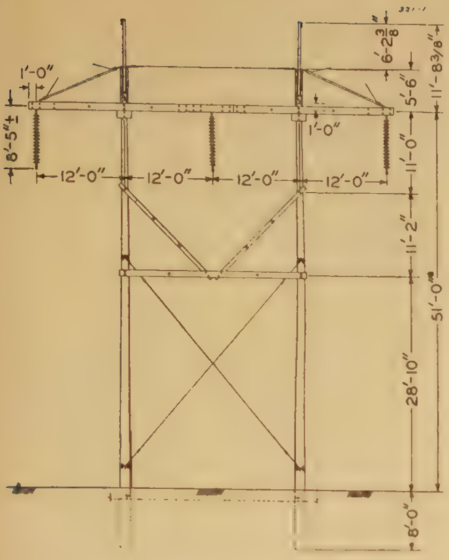

Figure 1. Type-*A* tangent structure

one-half-inch Siemens-Martin strands in the lower section. Each joint was carefully designed and assembled with bolts and timber connectors in an effort to attain complete rigidity of the structure in the direction of transverse wind loads.

Structure Types

Two circuits were built on a single right of way 250 feet in width, and center lines of structures of the two circuits were spaced 100 feet. Guying was so arranged that all guy stub poles and anchors were within the limits of the right of way.

The type-*A* tangent structure, shown in Figure 1, was installed at all normal tangent positions in the line and for small angle positions, guyed and unguyed, depending upon the angles of the line. The maximum angle with which this type of structure could be used was approximately three degrees. The point of minimum strength in the type-*A* structure is at the upper point of connection of the *K*-brace to the pole. For this reason, additional strength could not be obtained by guying from this point or from points lower on the structures. Therefore, guying at slight angles was accomplished by connecting to the end of the crossarm and extending the guy horizontally to a stub pole, then to the anchor connection at the usual angle. A wood strain insulator was installed in the horizontal guy between the crossarm and the stub pole in order to maintain the insulation level of the line. To provide transverse support for the overhead ground wires, a second guy was connected to the overhead ground-wire attachment on the bayonet. The two bayonets were connected horizontally by means of one-half-inch Siemens-Martin strand. Since pole ground wires were not installed on poles of guyed structures of any type, the overhead ground wires were connected to earth at such structure locations by means of the guy strands and anchor rods. Guys for type-*A* structures were installed on one side for small angles in the line and, similarly, on both sides for structures supporting long spans.

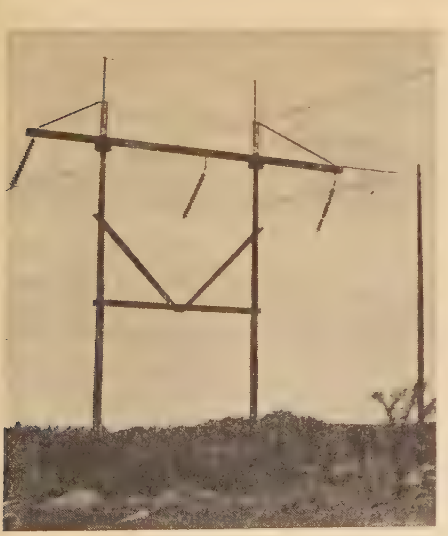

Figure 2. Type-*B* angle structure

The type-*B* structure, shown in Figure 2, is a variant of the type *A* in that the crossarm was offset two feet, and angle brackets were added in order to provide desired conductor clearances at angles in the line up to approximately eight degrees. Type-*A* structures were guyed also in the same manner as shown in this illustration. In relatively level sections of the right of way type-*A* and type-*B* structures were assembled on the ground and erected by means of special equipment shown in Figure 6. In rugged and hilly sections the use of this equipment was not feasible or economical. In these sections arms, braces, and all fittings were installed by means of hoisting equipment after poles had been installed.

The type-*C* structure, shown in Figure 3, is a braced and guyed angle suspension structure and was installed at positions where the line angle was between approximately 8 degrees and 25 degrees. The type-*C* structure is conventional, though enlarged for 230-kv operation, except that 12,000-pound wood strain insulators were installed as tension members to transmit overturning stresses from the tops of the unguyed poles to the guys.

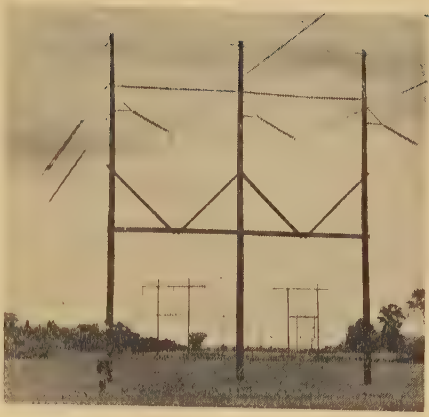

Figure 3. Type-*C* angle structure

Dead-end structures, referred to as type *D*, and shown in Figure 4, were installed for long spans, at line terminals, and at locations where the line angle exceeded approximately 25 degrees. At angle positions guys of sufficient strength to withstand maximum tensions of conductors and overhead ground wires were installed on both sides of the structures and in line with conductors and overhead ground wires, except that the overhead ground wire inside the angle at such positions was installed in suspension and held in place by one guy to the top of the guyed pole at the opposite side of the structure. This was done to avoid the use of stub poles and overhead guys. At tangent locations conductors were guyed in the same manner. In order to maintain clearances between conductors and overhead ground-wire guys at tangent locations, guys were installed in vertical planes displaced 45 degrees from the vertical planes of the conductors, and pole tops were connected together by means of one-half-inch Siemens-Martin strand in order to balance guying stresses.

Two sets of transposition structures were installed in each circuit to provide one complete barrel to balance the electrical characteristics of the three phases. Type-*F* structures were installed for this purpose, and are as shown in Figure 5.

At two locations it was necessary to cross high-voltage tower transmission lines of the power company operating in the area in which these lines were built. Crossing spans were supported in each case by type-*A* structures using 100-,

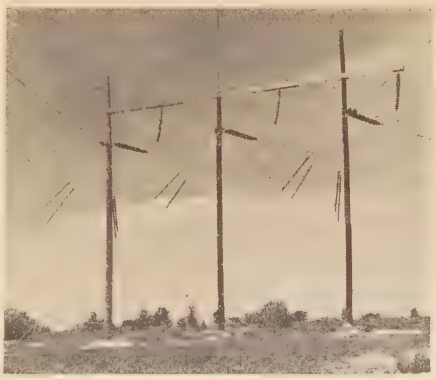

Figure 4. Type-*D* dead-end structure

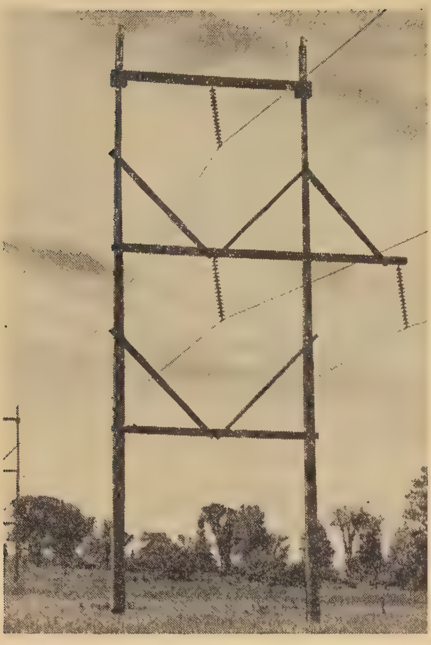

Figure 5. Type-*F* transposition structure

105-, and 110-foot pine poles. These structures were supported in all directions by means of sets of guys attached at the level of the horizontal braces and at points midway between these attachments and ground. Two spans away in each direction from the crossing spans, dead-end structures of normal height were installed, conductor heights having been graded down by the installation of poles of moderate lengths for the intermediate structures.

Guyed structures were installed as required by the rugged terrain and may be round at frequent intervals. Storm guyed and stabilizing structures as such were not used. The longitudinal strength of the assembled arm used in the type-*A* structure is such as to equal the unguyed strength of the supporting poles. The effect of one broken conductor is to deflect torsionally the whole structure, which deflection is resisted and limited by the remaining unbroken conductors and overhead ground wires. The overhead ground-wire bayonets bend under this condition, and the tops of the suspension insulator strings, moving, permit rotation of the crossarm in the horizontal plane. The conductor tension on the side of the structure opposite the break is relieved as well by the displacement of the insulator string, approximately eight feet in length, attached to the broken conductor.

Grounding

Overhead ground wires were connected to earth at all unguyed structures by means of pole ground wires and helical coils installed on each pole of the structures. Pole ground wires and helices were installed on every guy stub pole, and overhead ground wires were connected to earth through guy strands and these pole ground wires. Where stub poles were not required, the overhead ground wires were connected to earth by means of guy strands and anchor rods. Helical-coil grounds were installed on the butts of all poles, and, where pole ground wires were not installed, grounding conductors were extended from the butts of the poles to within a few inches below the ground line. Time did not permit the installation of an adequate grounding system during the construction period, though ground coils were installed for use as parts of that system, to be installed later. Wood strain insulators were installed in all conductor guys. The ungrounded ends of all these guys and all adjacent hardware were securely bonded to the insulator attachments to prevent possible charring of wood and radio interference.

Counterpoise

During the construction period and before the conductors and overhead ground wires were installed, resistances to earth of type-*A*-structure ground installations were measured. In this way a complete survey was made of ground resistances throughout the whole length of the line. A counterpoise system based upon this survey was designed and installed immediately following completion of the overhead construction.

In sections of the line where resistances were 30 ohms or less, no counterpoise conductors were installed. In sections where ground resistances were highest, approximately 750 ohms, four conductors were installed along the right of way and parallel to its center line. Counterpoise systems of three, two, and one conductor, according to measured resistances, were installed in other sections of the line. Solid galvanized-iron conductors of size 5 Birmingham gauge were buried by means of cable-laying plows to depths of 18 inches. Cross connections were installed to connect together all counterpoise conductors and all existing structure ground installations.

At all dead-end and guyed structures, all grounds, which included anchor rods and helical-coil grounds were connected together to form special low-resistance grids, which were, in turn, connected to the counterpoise conductors by means of cross connections. The entire overhead ground-wire system then had been connected to the counterpoise system by means of pole ground wires of the unguyed structures and the overhead ground-wire guys of the guyed structures.

Table I. Design Data

Type of construction	Single-circuit wood pole braced, *H*-frame with bayonet extensions
Operating voltage (design)	230 kv
Total miles of line	158
Right of way width	250 feet
Horizontal separation between center lines of circuits on same right of way	100 feet
Level ground span	520 feet
Average span	500 feet
Maximum span	1,300 feet
Poles (kind and class)	Southern yellow pine, western red cedar, and Douglas fir, Class 2
Treatment	Western red cedar, butts only; others, 8 pounds full pressure creosote
Normal pole length	60 feet
Conductor and pole separation	24 feet
Crossarms	Fir timbers, spliced, 50 feet over-all
Pole ground wires	Solid copper number 4 American wire gauge
Minimum ground clearance	32 feet
Minimum clearance to structure	86 inches, 76 inches to wood members
Loading class	National Electrical Safety Code, Fourth Edition—heavy
Conductors	Hollow design, 1.1 inches diameter, 500,000 circular mils
Overhead ground wires	Three number 5 copper-covered steel strand 30 per cent conductivity
Conductor tension (maximum)	9,000 pounds (41.5 per cent of ultimate strength)
Ground-wire tension (maximum)	4,300 pounds (36 per cent of ultimate strength)
Insulators	Spaced $5^{3}/_{4}$ inches, 15,000 pounds combined mechanical and electrical strength
Suspension insulator assemblies	Single strings, 15 units
Strain insulator assemblies	Double strings, 17 units
Counterpoise	One to four conductors as required
Counterpoise conductors	Solid galvanized iron 0.22-inch diameter
Line construction started	February 15, 1942
Line construction completed	July 3, 1942
Counterpoise installation completed	November 20, 1942

Total Insulation

The normal impulse flashover voltage ($1^{1}/_{2}$x40-microsecond negative wave) of conductors to ground of all suspension structures is 2,425 kv, which insulation is obtained by means of 15 suspension insulators spaced $5^{3}/_{4}$ inches and a wood strain insulator equipped with a flash gap of five feet, in series between conductor and ground. The impulse flashover voltage from conductors to pole ground wires through air is also 2,425 kv approximately. Under conditions of extreme conductor displacement and minimum clearance the impulse flashover voltage to ground of these structures is

1,500 kv. Impulse level of dead-end structures was raised to 2,620 kv by installation of two additional insulator units.

Ground-resistance measurements have not been made since the counterpoise installation was completed, though it is believed that the resistance to ground of any two adjacent span sections of the counterpoise system is not more than 30 ohms. Under conditions of minimum clearance of conductors to ground, currents of 50,000 amperes through the structure would be required to cause flashovers. Under normal conditions currents of 80,000 amperes would be required to produce flashover conditions. Less than one interruption per 100 miles of circuit per year is expected.

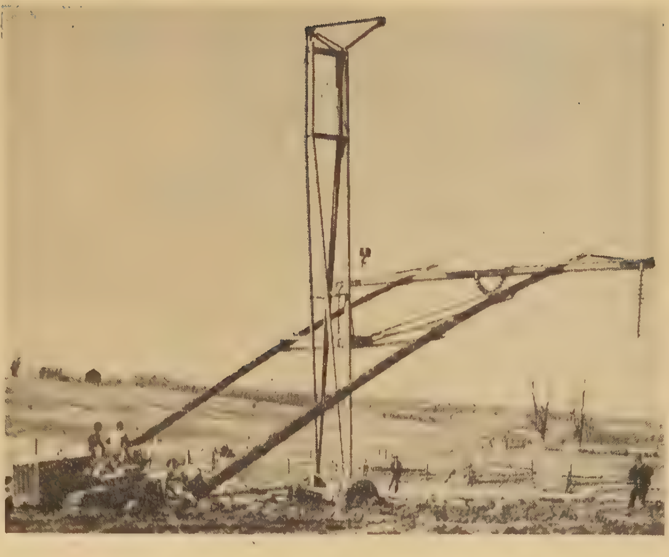

Figure 6. Special erection equipment

Conclusion

Circumstances which included an unfavorable season for outdoor construction and an extremely short construction schedule did not favor economical construction. Construction costs of these lines are not indicative of results that can be obtained under normal conditions. It is believed, however, that favorable costs can be obtained with wood construction for single-circuit 230-kv lines, though proof of this is not available at this time.

Time required for a thorough engineering study of the subject of 230-kv wood-pole line design was not available. The primary objective was to build a workable line within the shortest possible time and under circumstances which were adverse in many respects. Engineering studies, continued since construction work has been completed, suggest changes in details but indicate that the fundamental design is correct.

Wood construction for 230-kv transmission lines appears to be as practicable as for lower-voltage lines. Though steel may be available for construction of future 230-kv lines, it is believed that, in the interest of economy and improved operation, consideration should be given and further studies made regarding the use of wood supporting structures.

Switching Overvoltage Hazard Eliminated in High-Voltage Oil Circuit Breakers

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Synopsis: High-voltage transmission lines, when de-energized by conventional oil circuit breakers, are often subjected to high-voltage surges which present a real hazard to terminal equipment. The mechanism of surge generation is initiated by circuit-breaker restriking phenomena which are encouraged by the interruption of the large leading currents required to charge moderate and high-voltage lines. By interposing suitable resistors across the interrupting elements during the opening stroke of the breaker, the switching overvoltages may be either eliminated by preventing restriking or so reduced in magnitude in the event of restriking that the system is relieved of their hazard. The development of this achievement is presented chronologically:

1. Theoretical considerations
2. Transient-analyzer studies
3. Power-laboratory switching tests
4. Actual field tests on the 220-kv lines of Southern California Edison Company Ltd. at Boulder power plant

IT has been recognized for many years that some of the most severe switching surges encountered in the field are those which attend the interruption of line charging current. Tests on miniature systems have indicated that transient voltages of the order of six or seven times normal line-to-neutral crest may be obtained during such switching.[1-3] Field data, in general, substantiate such predictions.

In this paper a specific problem of high transient switching voltages on the 220-kv lines of the Southern California Edison Company Ltd. is discussed. The particular line which was being subjected to these overvoltages was initially operated as a single line with three ground-fault neutralizer coils located at Boulder power plant, at Barre substation, and at Chino substation.[4] At the present time another line is in service, and both lines are normally bussed at Boulder power plant and operated as part of the extensive 220-kv solidly grounded system. After diagnosing the symptoms from a theoretical standpoint, a complete analysis was made, using the transient analyzer to determine the possible magnitudes of switching overvoltages under various possible system-operating conditions. Various means of subduing these overvoltages were carefully considered, drawing on experience in related fields, and it was concluded that equipping the circuit breakers with internal resistors of the proper ohmic value afforded the best solution.

The paper presents the story of this development starting from theoretical considerations. Some results of analytical studies are presented as well as power-laboratory test results. A large portion of the paper is devoted to the field testing at Boulder power plant and interpreting the results obtained.

The effectiveness of the method described in controlling the switching overvoltage hazard is significant, not only from the circuit breaker standpoint, but from the arrester, relaying, and over-all system-operating standpoint as well, since lines and terminal equipment are freed from the possibility of being subjected to destructive switching overvoltages, regardless of the method of system neutral grounding.

Conclusions

1. Transient overvoltages due to switching can be reduced to harmless values by equipping the circuit-breaker interrupters with internal resistors of the proper ohmic value (see Figure 11).

2. While it has been previously recommended that the ground-fault neutralizer be short-circuited during switching operations at these high operating voltages, these tests show conclusively that this short-circuiting operation is not necessary when the breaker is equipped with such resistors.

3. From the standpoint of the magnitude of switching overvoltages, it makes no practical difference whether the system is in tune with the neutral reactance or not when switching is done by means of breakers equipped with such resistors.

4. Holding the transient switching voltages to moderate levels reduces the arrester duty to that of discharging lightning surges alone, thus minimizing arrester operations.

5. When interrupting fault currents, oil circuit breakers equipped with such resistors experience reduced power arcing as a result of the reduced rate of rise of recovery voltage (see Figure 8).

6. The major results of the tests at Boulder power plant confirm earlier conclusions reached through theoretical approach followed by a complete transient-analyzer study and power-laboratory tests.

Theoretical Considerations

Since the phenomena of the interruption of leading (capacitive) currents at power-system voltages are encountered when de-energizing (1) transmission lines, (2) cables, and (3) capacitor banks[5] (see Figure 1), it should prove profitable to review the fundamental nature of the phenomena in order to grasp a working knowledge of the problems. In so doing, it will be compared in its essential differences with the interruption of lagging (inductive) currents of the short circuit. Terms and concepts will be revealed in this section. The study of this section however is not imperative to the knowledge of the results of the paper.

From the outset, a group of extremely contrasting relations arise. First the lagging current of the short circuit deals primarily with heavy currents which approximate the full interrupting rating of the breaker. All leading currents, however, are relatively small, and, consequently, the light current characteristics of the breaker are involved. In the tank-type oil breakers these differences are largely hydraulic, the interrupters being designed to do their most effective work at heavy currents. Hence the interruption of any leading currents by the tank-type breakers does not profit by the insulating characteristic provided by the most effective oil-blast action. Circuit differences are so great, however, that the above features become of secondary importance.

The Inductive Interruption

A representative interruption of the inductive current is shown in Figure 2. Here, following contact separation, attempts at interruption occur at the first two current zeros. Each time the arc was *rekindled* by the rapid rise of recovery voltage across the breaker, approaching twice normal crest in a matter of microseconds. With the rekindling of

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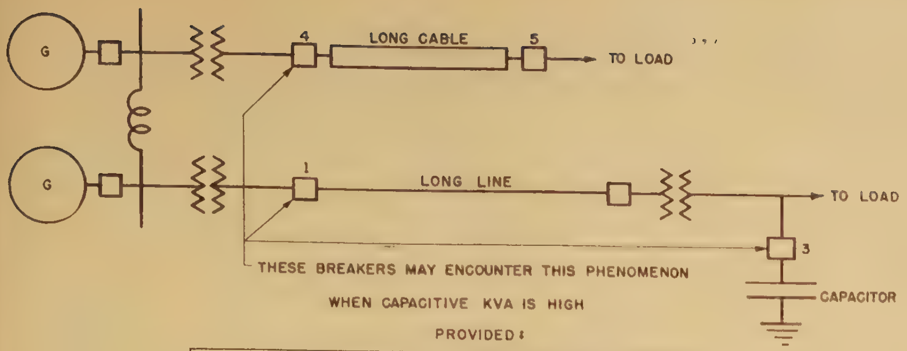

G
4
LONG CABLE
5
TO LOAD
G
1
LONG LINE
TO LOAD
3
THESE BREAKERS MAY ENCOUNTER THIS PHENOMENON
WHEN CAPACITIVE KVA IS HIGH
PROVIDED:
CAPACITOR

WHEN OPENING	BREAKER
1	2 IS OPEN
3	1 AND 2 ARE CLOSED
4	5 IS OPEN

Figure 1. Principal circuit conditions which may give rise to overvoltages of the type described in this paper

the arc, the accumulated circuit effects are completely liquidated at each current zero, and the current continues anew, as if nothing had happened, to repeat the attempt at the next current zero under more favorable conditions of contact parting and gap insulation. The characteristic of liquidating all accumulated debts at each current zero is a desirable property of the inductive circuit.

The Capacitive Interruption[2,5]

When the first current zero in the arc arrives in the passage of a leading current, interruption occurs quite easily. This comes about since the capacitive load retains the instantaneous voltage of the source at the instant of current zero, and for several hundred microseconds following there is little or no voltage across the breaker. The breaker thus mirrors an overconfident attitude in its ability to interrupt the circuit with the extremely small contact separation associated with the first current zero in the arc. All debts have not been liquidated, however, as one-half cycle later (see Figure 3) the bus voltage has reversed, and approximately double voltage thus appears across the breaker. If the breaker passes this peak without *restriking*, as does happen at limited kilovolt-amperes (short lines), then the confidence is justified, and no disturbance occurs. At higher kilovolt-amperes, however, when the arc develops more disturbance, the short arc gap cannot withstand the double voltage, and restriking occurs. At the time of restriking, the capacitor finds itself in the embarrassing position of being charged opposite to the desires of the bus voltage. In its eagerness to adjust the situation, a flow of current occurs which must be limited by the inductance of the circuit and, in good oscillatory style, permits the voltage to overshoot its mark by the initial difference. In this manner the voltage reaches minus $3E$ at the exact instant that the transient current passes through zero.

From this point two possibilities of major interest arise (Figure 3), namely:

(*a*). Interruption of the high-frequency current at the first current zero (case 1).

(*b*). Continuation of the high-frequency current, permitting it to die away, resulting in the re-establishment of the normal frequency capacitor current (case 2).

If the latter occurs, then interruption will again take place at the next current zero of the normal frequency current which is just one cycle after the first current zero in the arc. By this time the gap insulation will have increased, and subsequent restriking is usually avoided; hence final interruption is achieved. In this case the voltage to ground may rise to only three times normal crest on a single-phase basis. If on the other hand, the breaker permits the interruption of the high-frequency current at the first current zero following restrike, then the capacitor or line remains charged to as high as three times normal voltage crest. Then one-half cycle later, when the bus voltage again reverses, it permits a maximum of four times normal voltage to appear across the gap insulation. Subsequent restriking usually can be expected under these very severe conditions. In this manner voltages up to insulation breakdown values can be generated. The reduction of these overvoltages is the subject of this paper.

The above rough analysis of the interruption of the capacitive circuit, although more descriptive for the capacitor, applies equally well for the distributed case of the long open-circuited high-voltage line or cable. Here the voltage oscillations are more square-topped because of the traveling wave property of the load. As the line, for example, remains charged at the crest value of the voltage with the early interruption of charging current, double voltage then appears across the breaker with the reversal of the source voltage. If restriking occurs at the peak of this reversal, a voltage wave equal to the difference, or twice normal voltage crest, travels down the line to establish a charge equal and opposite to the initial trapped charge. The breaker current associated with this restrike is equal to the voltage of the traveling wave divided by the surge impedance of the line, about 500 ohms. When these waves reach the far end of the open line, the voltage wave reflects without change of sign to sweep back to the breaker, establishing a charge of approximately three times normal voltage crest. The accompanying current wave, however, reflects with change of sign to cancel the forward wave current. Hence current zero literally travels back to the breaker. When it arrives, the breaker arc is usually extinguished, thus leaving the line charged close to three times normal voltage crest. On a 200-mile line the complete round trip of such a wave from restrike to current zero at the breaker requires about 2,000 microseconds or about an eighth of a sixty-cycle wave. On a 10-mile cable the round trip requires about 200 microseconds.

Resistors

When resistors of the proper ohmic value are placed across the interrupting elements in the breaker, they function to reduce the probability of restriking by allowing the voltage on the load side to follow more closely the voltage of the source (see Figure 4). The resistors not only reduce the net voltage across the breaker but also act to distribute this voltage more equally within the breaker, thus lowering the maximum voltage per break which in turn further reduces the probability of restriking. It follows that should restriking occur (see Figure 4, case II), the net voltage difference (which defines the amplitude of the voltage oscillation about the generated voltage) is reduced, and hence the peak voltage to ground is lowered accordingly. To this reduction should be added the additional lowering of the peak voltage afforded by the damping attributable to the resistor. The combination of these several effects

1. Makes possible a higher capacitance kilovolt-amperes that may be switched with a given breaker without restriking.
2. Reduces the hazards of restriking in the kilovolt-amperes region where restriking is more probable.

The first of these advantages is the primary objective of the addition of resistors

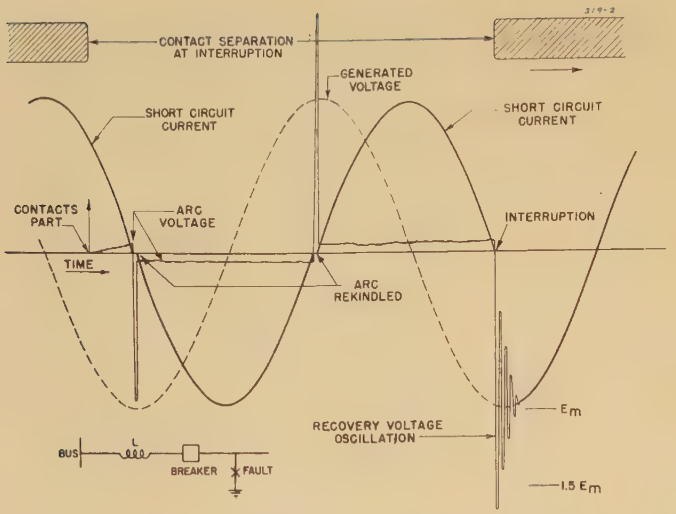


Figure 2. Typical interruption of an inductive short circuit showing arc rekindling at early current zeros

Compare with the interruption of the capacitive circuit shown in Figure 3

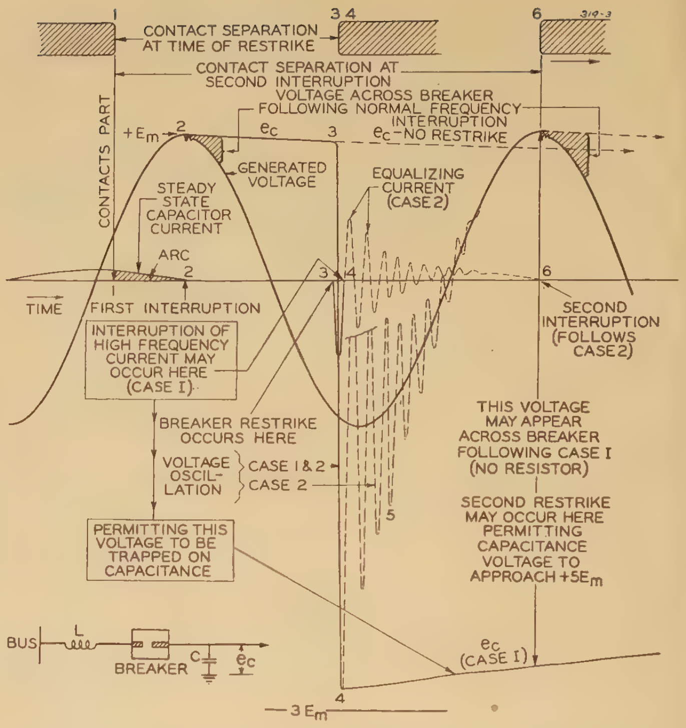

1
3 4
6
3/9-3
CONTACT SEPARATION AT TIME OF RESTRIKE
CONTACT SEPARATION AT SECOND INTERRUPTION
VOLTAGE ACROSS BREAKER FOLLOWING NORMAL FREQUENCY INTERRUPTION
CONTACTS PART
+Em
2
ec
3
ec–NO RESTRIKE
GENERATED VOLTAGE
STEADY STATE CAPACITOR CURRENT
EQUALIZING CURRENT (CASE 2)
ARC
2
1
3
4
6
TIME
FIRST INTERRUPTION
INTERRUPTION OF HIGH FREQUENCY CURRENT MAY OCCUR HERE (CASE I)
SECOND INTERRUPTION (FOLLOWS CASE 2)
BREAKER RESTRIKE OCCURS HERE
VOLTAGE OSCIL-LATION
CASE 1 & 2
CASE 2
5
THIS VOLTAGE MAY APPEAR ACROSS BREAKER FOLLOWING CASE I (NO RESISTOR)
SECOND RESTRIKE MAY OCCUR HERE PERMITTING CAPACITANCE VOLTAGE TO APPROACH +5Em
PERMITTING THIS VOLTAGE TO BE TRAPPED ON CAPACITANCE
BUS
L
BREAKER
C
ec
ec (CASE I)
4
—3 Em

Figure 3. Capacitive circuit interruption which shows a breaker-restriking phenomenon together with two possibilities of eventual interruption

This figure with the modification shown in Figure 4 is the theme of this paper

to breakers required to interrupt heavy capacitive kilovolt-amperes.

The resistor current is interrupted within the breaker as a plain-break interruption, care being taken that the resistor is so proportioned that the voltage surge associated with this final interruption does not exceed twice normal leg voltage crest. The design and arrangement of the resistor will be discussed later in the paper.

Transient-Analyzer Results

In order to make a thorough analysis of the effectiveness of shunt resistors in reducing the magnitude of switching surges, the transient analyzer[6] was used. The system studied in connection with this specific problem is shown in Figure 5. This miniature system is an approximate reproduction of the actual system at Boulder power plant on which the final field-test data were obtained.

By means of a synchronous switch which has been previously described,[6] each phase could be switched in sequence, or restrikes could be imposed in any one phase during the clearing process.

Oscillograms of such restriking are shown in Figure 6a for the first phase to clear with the system neutral solidly grounded. Voltages across the switch contacts are shown to reach values between four and five times normal line-to-neutral crest with two restrikes without resistors. When resistors are used (4,600 ohms in this case), these voltages are substantially reduced, as shown by other oscillograms in the same figure.

Line voltages to ground are also shown in Figure 6b. It is evident from these oscillograms that the shunt resistors are effective in

1. Reducing the a-c voltage on a line being switched out before the final break is completed.
2. Reducing the d-c voltage left on a line.

Bus voltages to ground are shown in Figure 6c. With no restriking, the bus voltage at the source is seen to drop noticeably because of the interruption of leading current. However, if restriking takes place, rapid changes in bus voltage can occur which may reach relatively high magnitudes as shown. Voltages on the bus side may be considerably higher than these if the neutral impedance is high.

A representative indication of the severity of overvoltages is the voltage across the breaker contacts during interruption. Therefore, the generalized

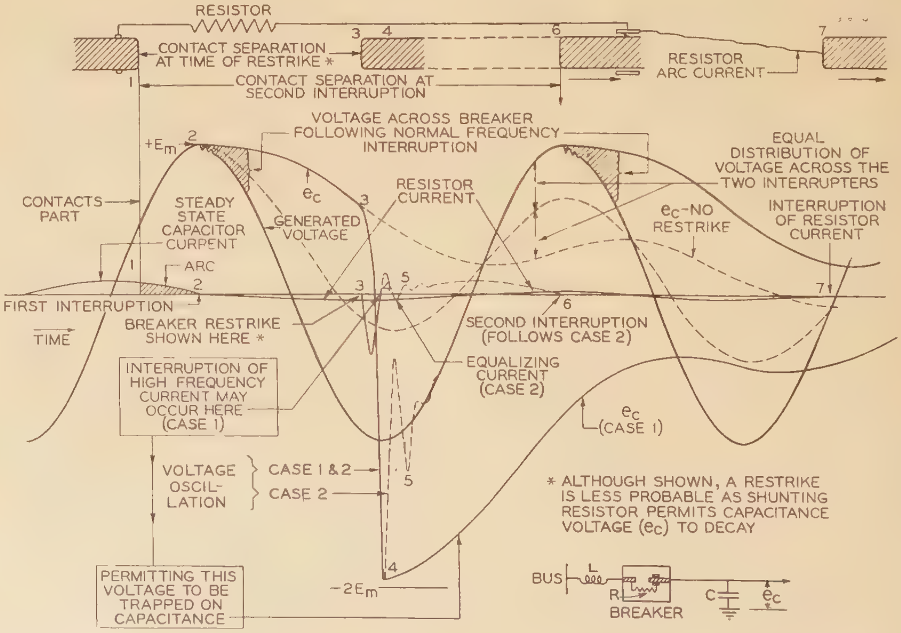

RESISTOR
3 4
6
7
CONTACT SEPARATION AT TIME OF RESTRIKE *
RESISTOR ARC CURRENT
1
CONTACT SEPARATION AT SECOND INTERRUPTION
VOLTAGE ACROSS BREAKER FOLLOWING NORMAL FREQUENCY INTERRUPTION
+Em
2
EQUAL DISTRIBUTION OF VOLTAGE ACROSS THE TWO INTERRUPTERS
RESISTOR CURRENT
ec
3
CONTACTS PART
STEADY STATE CAPACITOR CURRENT
GENERATED VOLTAGE
ec–NO RESTRIKE
INTERRUPTION OF RESISTOR CURRENT
ARC
1
2
3
4
5
7
FIRST INTERRUPTION
6
TIME
BREAKER RESTRIKE SHOWN HERE *
SECOND INTERRUPTION (FOLLOWS CASE 2)
EQUALIZING CURRENT (CASE 2)
INTERRUPTION OF HIGH FREQUENCY CURRENT MAY OCCUR HERE (CASE 1)
ec (CASE 1)
VOLTAGE OSCIL-LATION
CASE 1 & 2
CASE 2
5
* ALTHOUGH SHOWN, A RESTRIKE IS LESS PROBABLE AS SHUNTING RESISTOR PERMITS CAPACITANCE VOLTAGE (ec) TO DECAY
4
PERMITTING THIS VOLTAGE TO BE TRAPPED ON CAPACITANCE
–2Em
BUS
L
R
BREAKER
C
ec

Figure 4. Typical capacitive circuit interruption when resistors of proper ohmic value are inserted in the circuit during the interrupting period

Compare with Figure 3

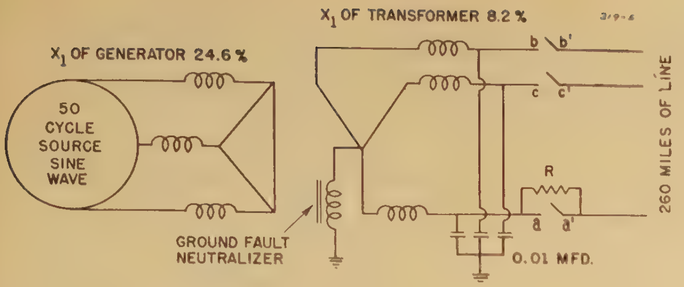


Figure 5. Miniature system studied with the transient analyzer the results of which are shown in Figure 6

curves of Figure 7 show this voltage as a function of the amount of shunting resistance R for various system conditions. It appears from these curves that there is no particularly critical value of shunt resistance required, but that any value of the order of 2,000 to 4,000 ohms would accomplish a very substantial reduction in overvoltage severity, regardless of the method of system grounding and for any phase to clear, that is, whether it is the first, second, or last phase to clear.

It can be shown that the effectiveness of a shunt resistor used in this manner depends upon the length of line. If a resistor is selected on the basis of sufficiently reducing transient overvoltages for the longest line that a given breaker is likely to be called upon to interrupt, then that resistor should be even more effective in reducing transient switching voltages for shorter lines, although the surge associated with the interruption of resistor current is likely to be slightly increased.

Laboratory Tests

The transient-analyzer solutions covering a wide range of probable circuit and breaker characteristics made another valuable contribution to the study by indicating

1. Critical circuit constants to be investigated.

2. The value of resistors which should prove most helpful in reducing overvoltages.

This information reduced the number of laboratory tests necessary, but even so over 400 tests were made in the power laboratory at Schenectady. However, the analyzer cannot indicate the restriking characteristics of the breaker, and hence power tests are essential. A study of the analyzer data indicated that, under the adopted assumptions with respect to breaker performance, a resistance between 2,000 and 4,000 ohms would be successful in holding the overvoltages, because of restriking, between 1.9 and 3.2 times normal over a practical range of circuit constants (including presence of ground-fault neutralizer).

Resistor designs were made up covering this range with the thermal requirements both on closing and opening under the worst field conditions in mind. The most satisfactory compromise, considering all factors, indicated a total resistance of 3,000 ohms per phase. Each resistor consists of a helically wound resistance wire which in turn is helically wound on a Herkolite tube. This winding is protected by an outer tube as shown in Figure 9. One resistor is connected in parallel with each interrupter as shown schematically in Figure 10. As the breaker opens, the power arc is interrupted in the interrupting elements, thus throwing the resistors (1,500 ohms apiece) in series with the line. As the crosshead (Figure 10) leaves the interrupting elements, the small unity power-factor resistor current is drawn as an arc between the external contacts and interrupted under plain-break conditions with a minimum of disturbance. The resistor also enters the circuit first when closing the breaker.

Leading current tests in the power laboratory were made by switching various banks of capacitors with a 230-kv oil circuit breaker, equipped with resistors as described above. Numerous tests were made also without resistors, although they were limited because of frequent arrester operations. Although the capacitor banks do not afford the traveling-wave phenomena associated with the long line, the switching problem is similar in all major respects. Therefore, the information obtained is applicable directly to the long line problem. The laboratory tests without resistors in the breaker did not reveal the true maximum voltages as protective arrester operations were encountered in many cases. The results of these tests demonstrated the ability of such resistors to hold switching overvoltages to very safe levels. This was particularly noticeable by the absence of arrester operations during tests in which the resistors were employed. Following the leading kilovolt-ampere tests the breaker, equipped with resistors, was tested on short-circuit interrupting duty

1. To test the mechanical features of the resistor element.

2. To observe the effect of the lower recovery rate; contributed by the resistor, on the arc duration.

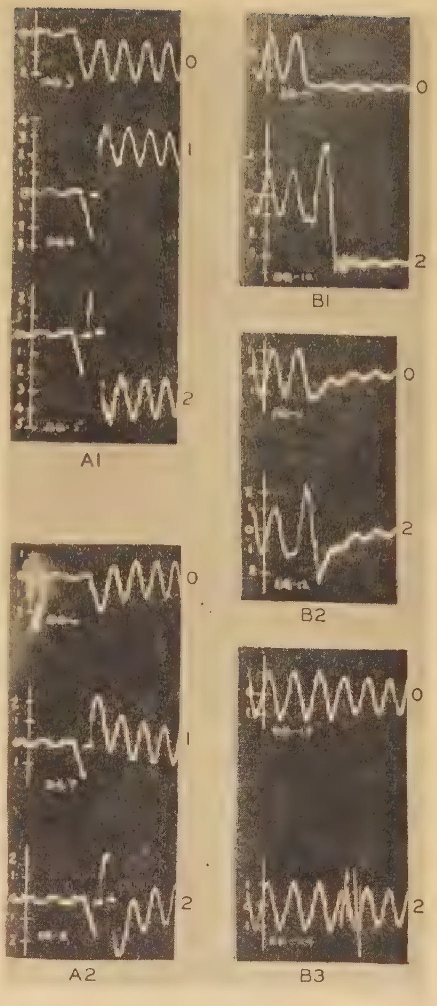


Figure 6. Transient-analyzer oscillograms illustrating the phenomenon of line-charging current interruption with and without shunting resistor in the miniature system of Figure 5

The ordinate is times normal line-to-neutral crest voltage

Numbers on oscillograms show number of restrikes in each instance

A. Voltage across the switch in phase *a*
*A*1. No shunting resistor
*A*2. 4,600 ohms shunting resistor
B. Line-to-ground voltage on line side of switch in phase *a*
*B*1. No shunting resistor
*B*2. 4,600 ohms shunting resistor
C. Line-to-ground voltage on source side of switch in phase *a*. No shunting resistor

Figure 8 shows the observed reduction in arcing time afforded by the reduced recovery rate. Breaker disturbance, contact burning, and oil carbonization are also reduced. The success of these tests led to the desirability of field confirmation on the Boulder circuits.

Field Tests

Such a series of field tests was made during the weekend of March 27 to 30,

Table I. Boulder Power-Plant Line-Dropping Tests—230 Kv

Test No.	Miles Line	Resistors	Neutral Reactor	Circuit Connection	Maximum Voltages*—Times Normal Crest									Crosshead Travel in Inches From Contact Separation to Interruption of Last Phase	
					Bus-to-Ground			Line-to-Ground			Across Breaker			Inside Interrupters	Final Break (Stroke = 36 Inches)
					A	B	C	A	B	C	A	B	C		
1A	132	Out	Short-circuited	2 generators 2d line	1.6	1.1	1.1	2.0	1.0	1.4	3.0	1.5	2.6	2¾	
1B					1.1	1.1	1.2	1.3	1.3	1.3	2.2	2.0	2.2	2½	
1C					1.0	1.0	1.0	1.0	1.0	1.2	2.0	2.0	2.1	1	
1D					1.8	1.1	1.1	2.0	1.4	1.1	2.4	2.4	2.0	1⅝	
1E					1.1	1.1	1.1	1.6	1.6	1.4	2.7	2.0	2.6	2¼	
2A	233	Out	Short-circuited	2 generators 2d line	1.1	1.1	1.0	1.6	1.3	1.6	2.4	2.2	2.3	2⅝	
2B					1.1	1.1	1.1	1.3	1.2	1.7	2.2	2.4	2.5	2⅝	
2C					1.3	1.1	1.6	1.2	1.4	1.9	1.8	2.4	3.0	2¾	
2D					1.6	1.2	1.5	2.2	1.2	1.8	3.0	2.0	3.0	3⅜	
2E					1.1	1.2	1.2	1.4	1.7	1.7	2.4	2.4	2.2	2½	
3A	132	In	Short-circuited	2 generators 2d line	1.0	1.0	1.2	1.0	1.0	1.0	1.0	1.0	1.0	1⅛	10⅛
3B					1.0	1.0	1.1	1.0	1.0	1.0	1.0	1.0	1.0	1	9½
3C					1.0	1.0	1.2	1.0	1.0	1.0	1.0	1.0	1.0	1⅛	9
3D					1.0	1.0	1.2	1.0	1.0	1.0	1.0	1.0	1.0	1	9¼
3E					1.0	1.0	1.2	1.0	1.0	1.0	1.0	1.0	1.0	1	11½
4A	233	In	Short-circuited	2 generators 2d line	1.3	1.0	1.1	1.5	1.1	1.2	1.5	1.4	1.0	1¾	9⅞
4B					1.0	1.1	1.1	1.1	1.1	1.1	1.1	1.2	1.3	1⅛	9¼
4C					1.1	1.1	1.6	1.3	1.1	1.6	1.4	1.1	1.5	1¼	10⅞
4D					1.0	1.1	1.1	1.1	1.0	1.0	1.1	1.4	1.0	⅞	8¾
4E					1.0	1.1	1.1	1.0	1.1	1.0	1.5	1.3	1.1	1	8½
5A	132	In	Tap 7	1 generator	1.6	1.6*	2.0*	1.5	1.2	1.0	1.4	1.2	1.0	1¾	14
5B					1.7*	1.4*	1.5*	1.0	1.3	1.1	1.0	1.0	1.0	1⅜	11⅞
5C					1.7	1.7	1.7*	1.7	1.0	1.0	1.6	1.1	1.1	2½	13⅞
5D					1.6*	2.0*	1.5*	1.2	1.0	1.2	1.1	1.0	1.0	1¼	14¾
5E					1.9*	1.5	1.5*	1.7	1.1	1.0	1.6	1.2	1.0	2½	14
6A	233	In	Tap 7	1 generator	1.6	1.6*	1.5	1.5	1.5	1.2	1.8	1.6	1.3	2¾	12¾
6B					1.5	1.5	1.4*	1.2	1.5	1.3	1.3	1.5	1.2	2¾	12¾
6C					1.4	1.7	1.4	1.3	1.4	1.3	1.2	1.5	1.2	2¾	11⅞
6D					2.0	2.0*	1.4*	1.6	1.0	1.0	1.5	1.4	1.0	2¾	12⅜
6E					1.4*	2.0	1.6	1.1	1.7	1.0	1.3	1.6	1.2	2¾	10½
6F	233	In	Tap 3	1 generator	1.1	1.6	1.4*	1.2	1.4	1.3	1.5	1.4	1.3	2⅝	11¼
6G					1.2*	1.6	1.7	1.1	1.6	1.5	1.4	1.5	1.4	2⅞	11⅞
7-1-A	132	Out	Short-circuited	1 generator	1.1	1.1	2.0	1.1	1.1	1.7	1.1	1.9	2.7	2⅞	
7-1-B					1.1	2.0	1.9	1.1	2.0	1.9	1.5	2.6	2.4	3⅛	
7-1-C					1.2	1.3	2.1	1.2	1.5	1.7	2.0	2.3	2.6	3⅛	
7-1-D					1.2	1.4	1.5	1.1	1.2	1.1	1.5	2.1	1.8	3	
7-1-E					1.3	1.2	1.2	1.3	1.1	1.3	1.6	1.9	2.3	2⅝	
7-2-A	132	Out	Tap 9** Tap 10** (Tuned system)	1 generator	2.4	2.2	2.2	1.8	2.0	2.7+	2.1	2.9	3.2+	4¼	
7-2-B					3.6	2.8	2.7	2.4+	1.6	1.6+	3.5+	3.2	4.3+	5	
7-2-C					2.6	2.3	2.2	1.6	1.6	2.1+	2.2	3.1	2.4+	4¾	
7A	132	Out	Tap 7	1 generator	2.9	4.4+	3.2	2.1	4.4+	3.2+	3.3		4.6+	6	

* Maximum voltages to ground recorded for both the bus and the line side of the breaker include voltages encountered either in the course of the primary interruption within the interrupters or in the course of the interruption of resistor current by the external break (see Figure 10). Voltages encountered in the interruption of resistor current are marked by an asterisk. For maximum voltage across the breaker only voltages occurring in the course of the primary interruption are recorded, since voltages across the breaker during the interruption of resistor current involve no stress on solid insulating material other than that occasioned by voltages to ground on either side of the breaker. Voltage across the breaker during the interruption of resistor current never exceeded twice normal line-to-neutral crest.

+ Reading affected by flashover in voltage divider. Approximate correction applied.

** Two reactors were connected in parallel; one on tap 9 and one on tap 10.

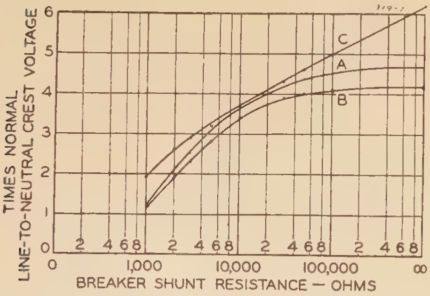


Figure 7. Effect of breaker resistors in reducing transient overvoltages during switching

Voltages shown are the maximum possible across the breaker for two restrikes in the phase being switched for any phase to clear, that is, first, second, or last

A—System neutral solidly grounded

B—System neutral grounded through ground-fault neutralizer tuned to 260 miles of line

C—System neutral grounded through ground-fault neutralizer tuned to half (130 miles) of the total of 260 miles being switched

1942, on the 230-kv number 8 oil circuit breaker in the switchyard of the Southern California Edison Company at Boulder power plant. This breaker is a tank-type breaker which contains multibreak interrupters[7] and has an interrupting rating of 2,500,000 kva and an interrupting time rating of five cycles. The voltage-limiting resistors, previously described, are mounted on the interrupters as shown in Figures 9 and 10.

All tests were circuit opening in which only the charging current of the connected line (far end open) was interrupted—commonly termed line-dropping tests. In this type of interruption, leading currents involved are small. Thus, within reasonable limits, arcing time is no important consideration. On the other hand, unless precautions are taken, breaker restriking quickly generates overvoltages which may be harmful to the breaker itself and to other connected equipment. Control of these overvoltages was therefore the primary concern in these tests, and the purpose of the test program was to determine

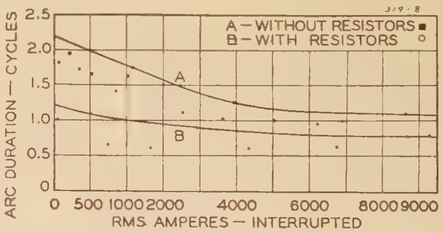


Figure 8. Breaker arcing time during the interruption of inductive short circuits, showing the improvement as a result of reduced recovery rates and better internal voltage distribution brought about by resistors in the breaker

1. The nature and magnitude of the overvoltages under various system conditions.

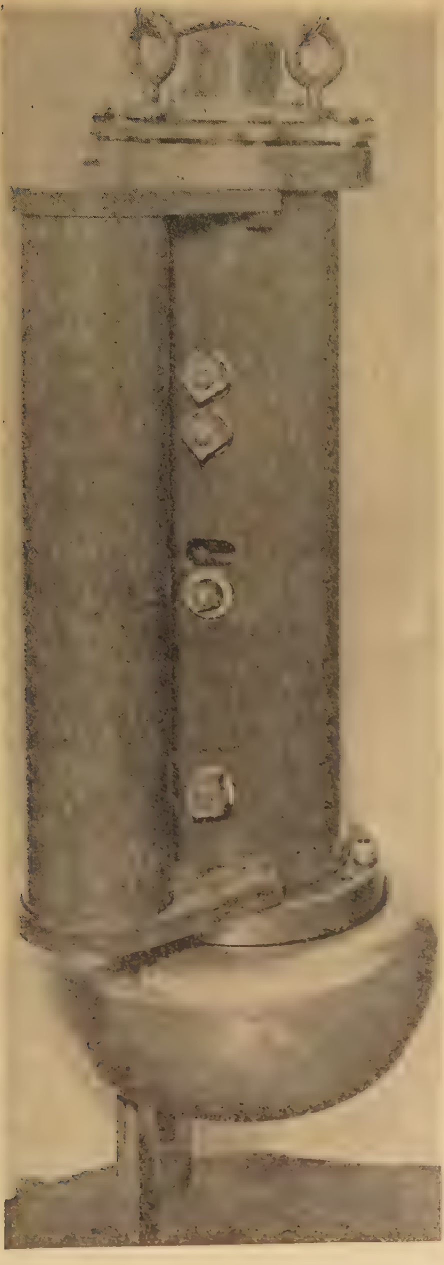
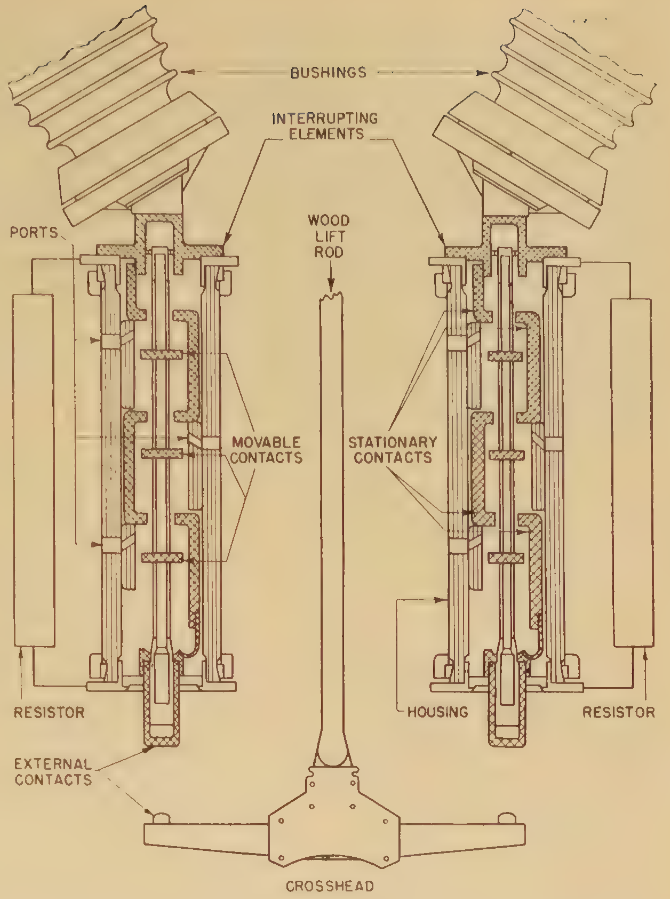


Figure 9 (left). General Electric multibreak interrupter unit for 230-kv oil circuit breaker with resistor attached

Two such interrupters are used per pole as shown in Figure 10

Figure 10 (right). Schematic diagram showing the multibreak interrupters with resistors attached

The resistor current is interrupted in the gap formed by the external contacts



2. The effectiveness of resistors assembled in the breaker, in parallel with the interrupters, in reducing these overvoltages to harmless values.

This approach naturally required that the line-dropping test program should include various practical system conditions with and without resistors in parallel with the interrupters. The following conditions were subjected to change:

1. The length of line being disconnected.
2. The neutral connection.
3. The nature of the power source.

Circuit Conditions

1. In some tests the entire 233-mile line to Chino substation was disconnected at Boulder power plant; in others only the 132-mile section to Pisgah substation.

2. In some tests the neutral was solidly grounded; in others it was grounded through reactance. Three different values of reactance were used; one of these values was that required for tuning as a ground-fault neutralizer to the line length being disconnected; the other two values, corresponding to other neutralizer tap settings, have been used to tune the line in conjunction with additional reactors at the far end of the line and consequently correspond to reactances above the tuned value.

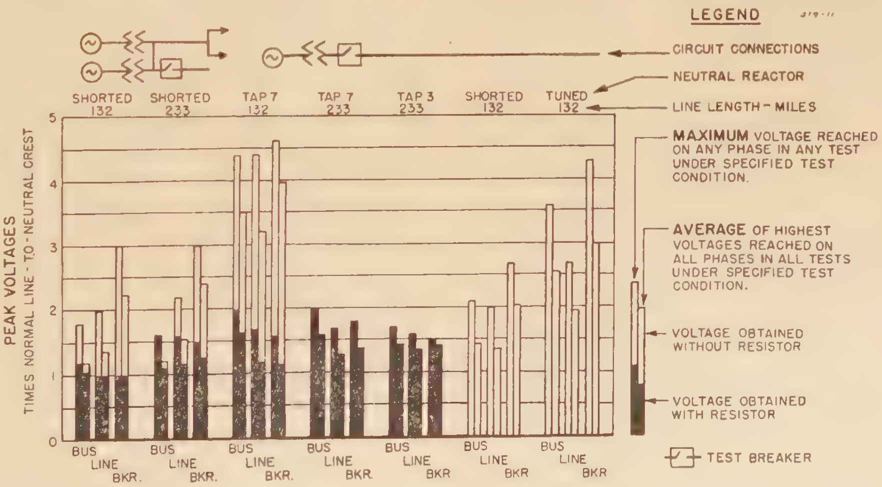
3. In some cases two local generator-transformer units were connected to the bus, and the remainder of the system was tied in through the second line to Chino substation. In the others only one generator-transformer unit was connected to the bus, and there was no tie with the rest of the system.

Test Results

A total of ten groups of tests was made at Boulder power plant covering practical combinations of circuit and breaker arrangement. Table I describes the results of these tests with respect to the maximum voltages developed and with respect to the breaker crosshead travel necessary for interruption. The overvoltage record, with and without resistors, is presented graphically in Figure 11 in such a way that the effect of the resistors, of the

Figure 11. Pictorial summary of results of oil-circuit-breaker tests made at Boulder power plant March 28 and 29, 1942

Comparison of voltages caused by interruption of transmission-line charging current. Here the overvoltages obtained with and without breaker resistors, under various line and ground conditions are shown



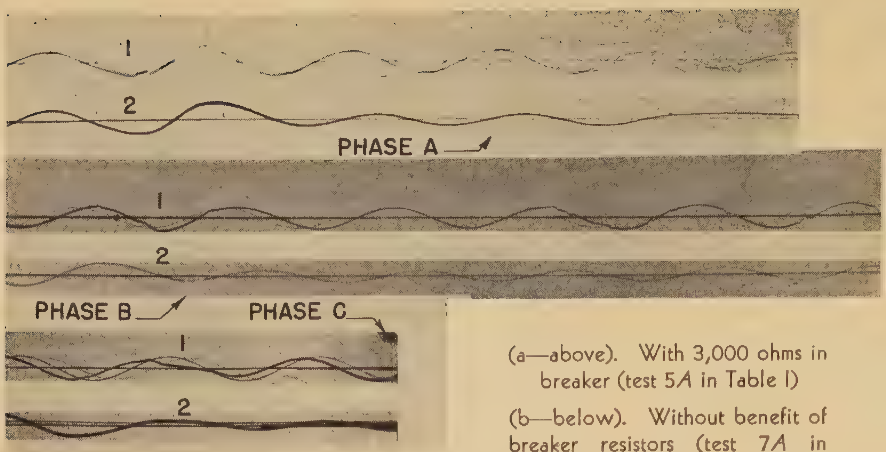
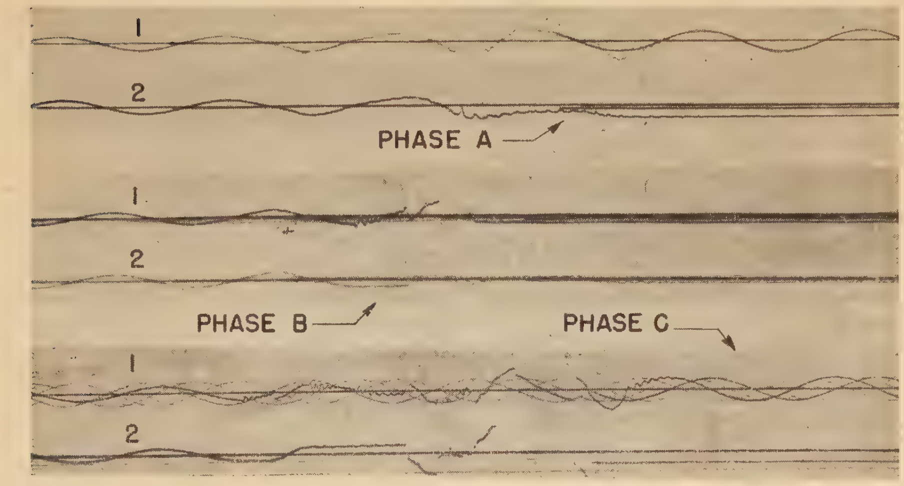


Figure 12. Comparative cathode-ray oscillograms of tests made at Boulder power plant when dropping 132 miles of 230-kv line with ground-fault neutralizer on tap 7

(a—above). With 3,000 ohms in breaker (test 5*A* in Table I)

(b—below). Without benefit of breaker resistors (test 7*A* in Table I). During this test a 220-kv bushing flashed over

1. Voltage to ground on bus side of breaker
2. Voltage to ground on line side of breaker



length of line, and of the ground-fault-neutralizer tap setting are readily apparent.

In the course of these field tests, three arrester operations were observed, one on phase *a* and two on phase *c*. These arresters are located near the terminals of the 82,500-kva transformer bank at Boulder power plant which is about one mile from the Southern California Edison Company switchyard. These three arrester operations all occurred during the tests which used the ground-fault neutralizer and no resistors in the breaker. During an earlier series of tests at Boulder power plant, at which time the above problems were being diagnosed, under similar system conditions (with the neutralizer untuned and without resistors in the breaker) six lightning-arrester operations were observed. No arrester operations were observed, however, for ten similar tests made at that time with the neutral solidly grounded.

The interrupters were completely examined at four points in the test program. There was no damage to the contact or insulation parts as a result of any of the tests made when the resistors were connected. On the last inspection following tests with the ground-fault neutralizer and without resistors (during one of which tests with the neutralizer untuned the breaker flashed over externally), there was some damage to the insulation parts of the interrupters but not of sufficient magnitude to prevent the successful interruption of the circuit.

Oscillograph Measurements and Records

A good illustration of the effectiveness of internal resistors in reducing transient switching overvoltages is shown in the cathode-ray oscillograms of Figure 12, which places together for easy comparison a test with a 3,000-ohm resistor in the breaker (Figure 12a) and a duplicate test without resistors (12b). These tests are numbers 5*A* and 7*A* respectively (Table I), in which 132 miles of line were dropped with the ground-fault neutralizer on tap 7. Figure 12a shows no overvoltages of any consequence although there is restriking during the clearing period. In contrast the restriking associated with Figure 12b (no resistors) results in violent transient disturbances which were so high as to cause sparkover of a protective gap in the cathode-ray-oscillograph measuring circuit which made it impossible to determine accurately the actual crest voltage obtained. The voltages reached for this test, however, were high enough to sparkover the 220-kv bushing on the source side of the breaker. This would require a maximum of 1,050-kv crest, depending upon several factors, the most important of which are the wave shape of the transient voltage and the lower humidity, both of which would lower the actual flashover.

The oscillograms of Figures 13a and 13b are the magnetic oscillograph records of the same field tests as presented in Figure 12, namely tests 5*A* and 7*A* respectively of Table I. In Figure 13a the duration of the unity-power-factor resistor current is clearly shown. Characteristic restrike current surges are shown in Figure 13b. Here four separate restrikes took place, all of which interrupted with the arrival of the first current zero of the high-frequency oscillation. Figure 13 also exhibits oscillograph measurements of phase current, trip coil current, and breaker travel.

Voltage to ground on each side of each pole of the test breaker was indicated on the cathode-ray oscillograph. The deflection plates of this oscillograph were energized by means of capacitance potentiometers, the upper section of which was furnished by the carrier-current coupling capacitors on the two transmission lines. This provided an arrangement capable of reproducing satisfactorily voltages of all types from the high-speed transients obtained upon a restrike in the circuit breaker to the slowly leaking charge left on the lines upon final interruption.[9] The two voltages for each pole of the breaker were recorded on parallel traces on the same film, so that the voltage across the breaker pole could be determined readily by graphical subtraction.

Discussion of Results

Figure 11 shows the effect of the resistors upon the overvoltages attending switching. As is indicated by this figure three pairs of test groups were taken in such a manner that direct comparison can

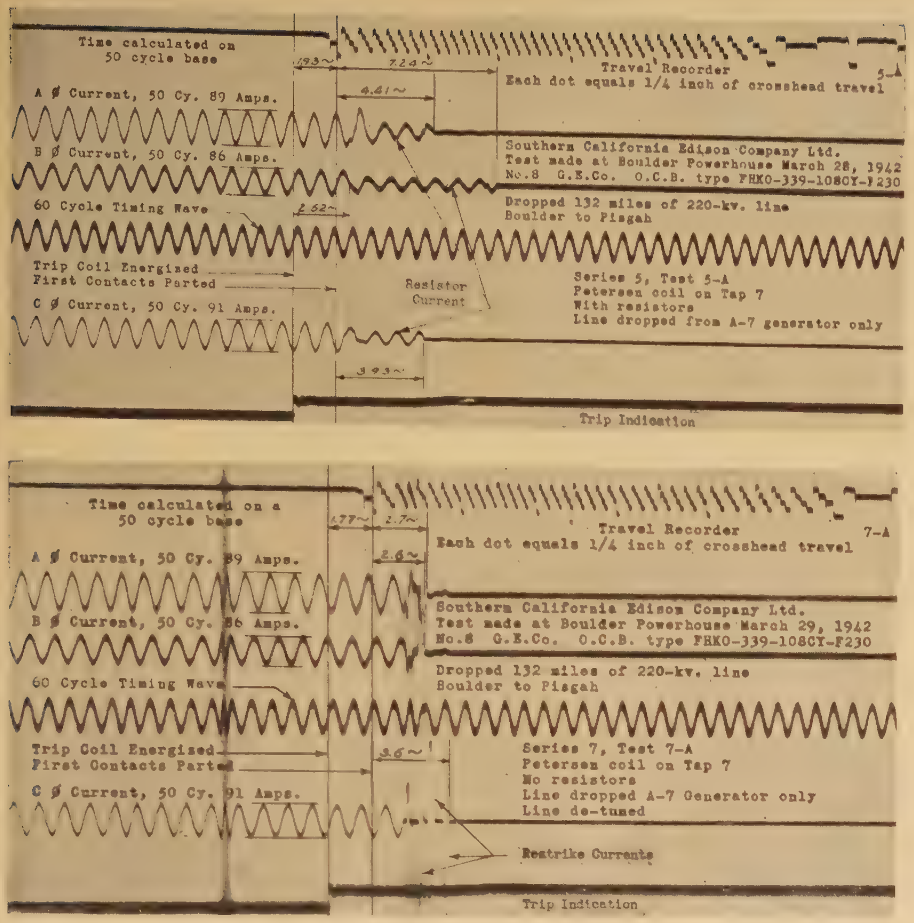


Figure 13. Magnetic oscillograms associated with the voltage records of Figure 12

(a—top). With breaker resistors. Note resistor current

(b—bottom). Without resistors. Four separate restrikes occurred on this test of the type shown in Figure 3, case I

be made to show the effect of the resistors. In these cases, and also in the other groups taken with the resistors connected, the voltage was limited to twice normal line-to-ground peak or less—both when the resistor was being switched in and when the resistor current was being interrupted. When the resistors were not connected, on the other hand, the voltage across the breaker reached three times normal with the neutral solidly grounded and more than four times normal with a neutral reactor connected.

Correspondingly, as shown in Table I, the resistors bring about a considerable reduction of arc length inside the interrupters as a result of the improved internal voltage distribution and the improvement in the rate of rise of recovery voltage. The residual current which remains in the resistors is then interrupted by the disconnecting break (formed by the crosshead, Figure 10) in from about 15 per cent to approximately 35 per cent of their available break distance. Here also the resistors are effective in preventing any build-up of voltage beyond the values shown in Table I and Figure 11.

The fact that the resistors produce such a substantial reduction in overvoltage makes them preferable to other measures, since they afford protection thereby to other terminal apparatus. Unless the breaker is sufficiently positive in action to prevent restriking and its associated overvoltages, therefore, the use of resistors appears desirable whenever the length and voltage of the lines are such as to give rise to trouble of this type. Because of the dependence of this restriking phenomena upon so many factors, the most important of which is the breaker characteristic, it is impossible to indicate here the capacitance kilovolt-amperes where restriking can be expected to occur. Tests at 15 kv on one manufacturer's breakers,[5] for example, indicated a limit of 10,000 capacitive kilovolt-amperes above which overvoltages could be expected without the aid of internal resistors. The 132 miles of line dropped, as exhibited in Figures 12 and 13, represents about 35,000 capacitive kilovolt-amperes. These values cannot be compared directly with those shown at 15 kv, however, because of the wide difference in voltage levels. The above makes clear that when small capacitive kilovolt-amperes are to be interrupted, with standard power breakers (short lines or cables, protective capacitors, and so forth), no trouble should be expected.

The operations in all the tests presented in this paper were confined to dropping a length of open transmission line, but, since breaker operation upon all except three-phase faults involves dropping at least one unfaulted phase conductor, the phenomena will be present in practically all breaker operations.

The results of these tests are in good qualitative agreement with the results of miniature-system tests as regards the principal features:

1. With the untuned neutral reactor, voltages obtained without the resistor were much higher than those obtained with the neutral solidly grounded.
2. The resistors brought about a substantial reduction of voltage in all cases, and, the higher the initial overvoltages, the greater was the percentage reduction.

A comparison of the overvoltages obtained for the tuned ground-fault neutralizer with those obtained for the solidly grounded system indicates the desirability of supplementing miniature test with full-scale tests under actual conditions. This is emphasized by the fact that the voltages associated with test series 7–2 are very high, relative to those obtained for test series 1, which was the solidly grounded case with the same line length. From earlier studies, on the other hand, it had been concluded on the basis of the transient-analyzer results that interrupting line charging current in a ground-fault-neutralizer system is no more difficult from a switching standpoint than in the same system with neutral solidly grounded. The reason for this disagreement apparently lies in the fact that surges communicated to one phase by clearing or restriking in another were of much more importance in the ground-fault-neutralizer tests than with the neutral solidly grounded. Not only did this have a direct bearing on voltage magnitudes, but in a number of cases it appeared to be directly responsible for additional restrikes which contributed still more to the overvoltages. In the grounded neutral tests there was never more than one restrike which contributed appreciably to the building up of voltage, but several times there were two in the ground-fault-neutralizer tests. Since the miniature-system tests were based on the assumption of two restrikes with the worst timing, the full-scale grounded-neutral tests produced overvoltages considerably lower than predicted. The ground-fault-neutralizer overvoltages were about equal to predicted values, the loss caused by the fact that the worst timing was not realized being approximately balanced by the increase arising from interphase reaction.

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TRANSACTIONS SECTION

Preprint of Corresponding Pages From the Current Annual AIEE Transactions Volume

Any discussion of these papers will appear in the June 1943 Supplement to Electrical Engineering—Transactions Section

Keep Them Rolling

J. W. TEKER
ASSOCIATE AIEE

THE electric locomotive of World War I days has broadened its usefulness to a position of recognized importance among the nation's available motive power now so necessary to the successful conduct of the present world's struggle.

This is largely attributed to the recent and swift growth of the Diesel-electric locomotive, essentially an electric locomotive carrying its own power plant, for both road and switching service.

Barely keeping pace with this new giant are the comparatively small but important groups of skilled men specializing in the care of this motive power and comprising a new branch of the established maintenance and repair department of steam railroads. The continued smooth and vital operation of transportation equipment pushed to its utmost depends upon these men; yet even these ranks are being thinned to supply the demand for skilled men in today's highly mechanized warfare.

The conservation of such highly strategic elements as time, material, and man power can find no better application than in the field of preventive maintenance. Like any preventive measure applied in time, it requires the minimum of time, effort, and facilities and can be accomplished fortunately by men with less training and skill than the more costly repair crew of high skill with necessary shop facilities.

Good generalship on the home front demands the application of the engineering method, based on fundamentals, to appreciate the nature of failures and then to guide and direct the efforts of available man power and facilities not only in repair but in the prevention of failures.

It is difficult to expound anything new in the field of maintenance, but it is possible, and the purpose of this article is to point out certain fundamentals underlying the most common maintenance functions applying to traction motors and generators. It is the expectation that such a review will stimulate responsible engineers and operators to examine and test their own practice in this light and establish preventive methods to keep the equipment rolling and out of the repair shop.

Supervisors confronted with the loss of expert workmen who carry routine practices in their heads or with the influx of new help must be especially on the alert for neglect of preventive maintenance methods and must get close again to the job to see that specific standing instructions are available and that schedules and records are not neglected.

The neglect of some simple detail by one man may start a chain of events to culminate in a serious breakdown and require an entire crew of men with shop facilities already sorely taxed to make corrections. The lack of only one-half minute's attention to cleaning a terminal may cause the contact surfaces to overheat and burn off when most heavily loaded, and a motor failure resulting when the motor is most needed brings a train in late with remaining motors overheated. This is sabotage, unintentional but effective.

Lubrication—The Oldest Problem of Maintenance

Lubrication constitutes one of the oldest and most common of maintenance functions and yet in spite of the available background provides many problems ranging from the selection of lubricant to the method and frequency of application.

Lubrication of electric traction equipment falls into three divisions, namely, sleeve bearings, antifriction bearings, and gearing. The basic purpose of lubrication in all of these is to separate the moving elements from each other by a film of oil which must support the entire load on these moving parts. Interruption of this oil film for any reason immediately allows contact between surfaces, and destructive wear begins.

SLEEVE BEARINGS AND OIL WICKS

Waste-packed sleeve bearings are common for traction-motor-support axle bearings. Fundamentally the waste packing is a wool-yarn oil wick which raises oil from the oil well to the journal surface through a window in the bearing lining. The space between the tightly packed woolen strands forms small capillary tubes, and it is between these strands and not through them that oil rises by capillary forces.

A properly packed bearing consists of two distinct parts, each having separate functions. The first is the wick portion of straight wool yarn placed vertically and evenly adjacent to the journal, one end extending down into the oil well and the other end left extending out until the second portion of the packing is in place. The second part is packed tightly in loops or folds, the purpose of which is to back up and compact the wick so as to keep it against the journal and to create the necessary small spaces for capillary action. The first portion of the packing, left extending, is now tucked securely on top of the packing so that the wick is able to resist drag in either direction of rotation.

The packing of bearings should be entrusted only to men trained and skilled in this operation. Fortunately the principle is simple, and once understood the skill is easily acquired.

New waste, clean and dry from the vendor, may be oil repellent and not con-

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ducive to good wick action. Therefore, it must be thoroughly oil soaked to insure complete wetting of all fibers and then drained before packing bearings with it.

Because of the very nature of wick action, it is important to select lubricants free from clots of tarry residue and sludge to keep from clogging the small capillary spaces between the strands and thereby restrict the free flow of oil up through the wick.

Winter and summer grades of oil must be considered where operation goes through the extremes of temperature, because an oil of sufficient viscosity to maintain an adequate oil film through summer operation is not likely to have a pour point sufficiently low in the extreme of winter operation to permit its flow through the wick to the journal surface. This invites lubrication failure until the heat from the dry bearing raises the temperature of the oil to restore flow. Meanwhile, wear has taken place in the bearing, or destruction occurs before lubrication is restored.

One grade of oil will do only where the seasonal temperature variation stays within the operating range of the lubricant. This is the subject of individual decision, based on local operating conditions, but the underlying fundamental remains unchanged.

When gauging oil level, especially during certain seasons of the year, condensation and water-soaked road beds cause accumulation of water in oil wells. The measuring stick may give a false indication of the true amount of oil in the well, because it will be wetted by even a small layer of oil floating on top of a large accumulation of water in the well. Periodic removal of drain plugs should be called for or samples may be obtained from the bottom of the well with a glass tube to check on water content.

A Prescription for Lubricating Roller Bearings

Antifriction bearings for supporting motor and generator armatures are almost universally grease lubricated. Roller bearings require a film of oil over the highly polished steel surfaces. More than this film is excess and leads to many known evils attendant with overgreasing. A roller-bearing compartment approximately two-thirds full of grease is ideal. This allows space for the grease to be thrown clear of the spinning bearing and yet provides ample grease reserve. When a bearing compartment is too full of grease, it is forced into the spinning bearing, and churning of the grease takes place, producing excess temperatures, rapid breakdown, and oxidation of the grease. The internal pressures built up within the bearing compartment under these conditions cause the grease to be expelled past the bearing seals into the motor.

The grease expelled from the commutator end bearing accumulates under the commutator shell and flows back to clog armature-core air passages. The restricted flow of ventilating air may increase the temperature of the winding dangerously.

Excess grease, thrown over the inside of a motor, accumulates enough dirt to initiate electrical failure. The presence of grease and oil on the commutator surface is detrimental to the carbon brush performance and leads to commutation difficulties.

Since it is difficult, if not impossible, to determine the grease content in the bearing housing when greasing from a pit, the importance of relying on a correct grease schedule is evident. The amount of grease added to each bearing and the frequency of application depend upon service conditions. It is important that a regular greasing schedule should be established only after first experimentally determining the correct quantity and the time interval for the particular service requirements.

Continuous operation at high speed such as in streamliner service makes greater demands on bearing lubrication than slow-speed switching. The life of grease is dependent upon temperature, and this is dictated by motor duty and cab temperatures. General greasing recommendations can be made, but these must be verified to see that they suit local conditions.

Stubborn arbitrary decisions lead to disaster in broken bearings, wrecked motors, flat wheels, and delayed trains. Only open-minded realism can avert it.

This is not so difficult to do as it first may appear. There are many occasions during the course of operation where motors are available for removal of bearing caps so that grease content can be determined accurately. A few such inspections quickly give an accurate picture of the adequacy of the schedule being enforced. Records of such inspections supplemented by other checks such as excess grease throwing or removing plugs and obtaining samples with a hooked wire enable operators to keep a "pulse" on actual lubrication needs and revise greasing schedules correctly.

Records and schedules must represent what is taking place in the greasing pit. All efforts may be nullified by carelessness and haphazard methods during the greasing operation. It must be known just how much grease is being delivered by the greasing equipment in use. The extra shot, just for good luck, repeated frequently will upset any supervisor's records.

A desire for a perfect grease is normal and healthy, but, before it is released with a train, determine which will fail first, the motor bearing or the grease. Repeated demonstrations have shown that overfilled bearings overheat in high-speed operation, and it is recognized that unless greasing schedules are carefully controlled, overgreasing is common.

High-temperature greases are available, but the steep temperature rise attendant with overgreasing can cause destruction of the bearing steel before the breakdown temperature of such grease is reached. When a grease melts at not over approximately 150 degrees centigrade, there is opportunity for it to escape through the labyrinth seals, ease the internal pressure, and bring about a reduction in bearing temperature before destruction occurs. The grease, after such experience, is damaged, but the motor remains unharmed while the grease may be easily renewed.

Precautions Extending Bearing Life

Cleanliness is of extreme importance in the operational life of the highly polished precision-built roller bearings. Allowances cannot be made for any condition under which a maintainer may have to work. When grit enters the bearing, it is helpless to resist its destructive action. There are no extenuating circumstances—dirt must be kept out.

Caps for grease fittings are available, or fittings can be wiped clean before applying the grease gun; otherwise grit will be forced into the bearing with the grease. Grease guns must be filled under conditions safeguarded against contamination.

A check on the tightness of fittings and grease pipes is a "must" in the regular inspection schedule. A loose pipe clamp will allow vibration of the pipe until it drops or breaks off, leaving an opening directly into the bearing housing for entrance of water and dirt.

The policy of removing motors on a mileage basis allows detection of defects and application of comparatively simple corrective measures which otherwise would result in the costly destruction of the motor and tie-up of operation. It supplies evidence from which errors in operation or maintenance can be corrected, whereas inspection after ultimate failure has occurred often finds such helpful evidence destroyed.

This provides opportunity to remove hardened grease residue from the bearing housing and to measure contents as a check on grease schedules. The bearing can be cleaned and carefully inspected to detect defects such as loose cage rivets, excess wear, chipping, and breakdown of race or roller surfaces.

When a defective bearing is found, the motor parts involved should be checked for alignment. This can be done when the motor is reassembled and before bearing caps are assembled. An indicator clamped to the armature shaft and rotated so that it bears on the face of the outer bearing race will check the alignment of the bearing with respect to the armature axis. This should be done on both commutator and pinion end bearings, together with a check on end play and bearing clearance before closing up the assembly. Such procedure will detect a frame or framehead that may have warped from overheating or impact and thereby hastened the breakdown of the original bearing. This will enable corrective measures to be applied before the motor is returned to service and thus prevent the damage of the replacing bearing.

Lubrication Is Essential to Gearing

Gearing is probably the least understood mechanism entrusted to maintenance men. Were it generally known what effort manufacturers put into the design, heat treatment, and shaping of each tooth to the thousandth of an inch, gearing would receive the respect and attention it deserves. Few maintenance men understand the basic principle underlying the operation of involute gearing most commonly used for traction application. There is a common misconception that teeth are shaped so that they may roll on each other. Actually, the teeth surfaces slide on each other, the roll being confined to a very small portion of the profile, and the tremendous forces of moving a train are transmitted to the drivers through this sliding contact between gear teeth. It is a vital necessity to separate this contact by a lubricating film; otherwise, destructive wear is sure and certain.

Gear lubricants in common use depend upon their adhesiveness to keep the tooth covered with a protective load-carrying film. This stickiness is essential as centrifugal force tends to throw the lubricant off the teeth. The lubricant is renewed as it dips into the quantity contained at a proper level in the bottom of the gear case. The properties of the gear compound must be such as to flow readily into the path of the rotating gear and yet be sticky enough to adhere to the tooth surface, and have load carrying ability. These properties are affected by temperature. Most compounds do not have sufficient range to cover the extremes in temperature encountered from summer to winter. Careful selection of proper grades for winter and summer operation must be made where wide temperature variations are encountered. The heavy summer grade will set up solid at low temperature, and the gear will channel through it without being wetted. A winter grade of thin lubricant during the heat of summer operation will fail to stick to the teeth at speed, or at heavy load is too thin to separate the gear teeth.

A few hours of heavy load running without adequate lubricant may lead to irreparable damage. Gearing will overheat and damage the heat treatment of the steel. Excess pinion temperature by conduction through the armature shaft can overheat the roller bearing. The unequal expansion of inner and outer races, absorbing the internal bearing clearance until loaded beyond limits by internal radial stress, can cause complete destruction of the bearing and frame heads, and even destruction of the armature core when it drops on the field pole pieces at high speed, all because of faulty gear lubrication.

A necessary adjunct to gear lubrication is the gear case or gear cover. Its two functions are to keep the lubricant in and to keep foreign material out. The tongue and groove joint between upper and lower halves, the baffles and seals around axle and pinion fit originally installed by the manufacturer are seldom maintained. This is so discouraging and futile that in some designs they are completely omitted.

Contamination of gear lubricant with material picked up from the road bed makes an excellent lapping compound with no chance for grit to settle out in the thick viscous lubricant, and the very object of lubrication, to prevent wear, is defeated.

Every time a gear case is dropped for wheel change or motor change-out it is an opportunity to renew seals, check gear teeth for signs of lubrication failure, and take necessary preventive measures.

Cog Wheels or Gearing: Which?

Fortunately, gearing has considerable power of recovering and after many abuses continues to transmit power even after its fundamental tooth profile has been destroyed and turned into mere cog wheels. Gear teeth seldom fail in service but continue to operate until attention is arrested by their appalling appearance, and the gears are changed out from sheer fright.

Too much attention is centered on the strength remaining in worn gear teeth from a breakage standpoint with a total disregard for the carefully designed shape of the tooth profile. There is a vast difference between cog wheels and gearing. The precise original shape of the involute tooth, while not necessarily the only possible shape, is to maintain uniform angular velocity between gear and pinion. Deviation from a proper shape for uniform angular motion for any reason makes smooth uniform rotation between pinion and gear impossible, and jerky power transmission begins, attended by severe instantaneous stresses and torsional vibration.

When gear, pinion, and axle linings remain mated over a long period, the wear is gradually adjusted between the members without causing serious deviation from normal operation. This may continue until bearing lining wear effects a gradual increase in gear center distance, and the gearing may also wear to a depth sufficient to produce a step in each tooth profile.

Now, if the gear center distance is restored by installing new linings, it is evident that the tooth tip will be caused to ride alternately up and off the step formerly worn into each tooth, and there is interference. The same occurs when a worn gear or pinion is mated with a new one. Tremendous stresses are set up at the interference point until the teeth are battered into agreement. While this is going on, the armature shaft, bearings, and the armature winding are enduring the punishment of bending and vibratory torsional stresses.

The interdependence between apparently unrelated parts is startling; lubrication failure in gearing or axle linings may be followed by a broken armature shaft or a grounded armature winding resulting from vibratory stresses beyond the limits of the construction during the breaking-in period of unrelated gearing.

As soon as operators recognize the characteristics of true gearing, they will give immediate consideration to such practice as keeping gear and pinion mated even though the motors themselves are changed around. When a motor is changed out, the pinion is removed and installed on the ingoing motor, thereby keeping the same pinion and gear running together. In order to retain existing gear center distance, the linings also should be removed and installed on the ingoing motor.

When worn gearing must be matched with new, or where interference in tooth profile may result for any reason, consideration should be given to having the worn gearing replaced immediately with the same dispatch that worn wheels are ruled off the road. Fundamentally the question resolves itself into a choice between the additional miles to be obtained from damaged gearing and the punishment being sustained by a costly traction motor transmitting its power through cog wheels no longer capable of their original gearing function. Where armature failures are frequent, this aspect of the problem is worthy of serious study in the interests of economy and availability.

Present-day facilities for pulling pinions, easily controlled means for heating pinions, and the measurement of correct advance simplify the pinion-mounting operation to such extent that the practice of keeping pinions and gears mated is simple, quick, and attractive in view of the benefit to be gained.

Precautionary Tactics From the Service Pit

INSULATION

There is much that can be done to forestall trouble in motors and generators while they are mounted in the locomotive. A regular check of insulation resistance with a "Megger" when a locomotive comes in from a run is a simple and quick operation; yet it may detect a defect which might result in a ground failure on the very next run.

Make high-potential tests sparingly and carefully, preceding them with a Megger insulation check; otherwise a puncture or creepage failure may result from the test which could have been avoided by finding first the weak spot with the "Megger" and cleaning, drying, or repairing locally as necessary.

BRUSHES AND BRUSH RIGGING

Care must be exercised when installing new brushes to prevent the snapping down of spring levers with such force as to chip or shatter the brush. Carelessness in this respect may ruin a brush before it has a chance to give useful service.

Install a new brush in such a manner that the pigtail is placed on the far side of the carbon way away from the side on which the pigtail terminal screw is located. This permits an easy loop to be made over the brush, with little chance of its being bent over the edge of the carbon way and thus hang a brush up so as to prevent free vertical movement. Where pigtails bear against the edge of the carbon ways or spring levers, there is rapid wear of the individual strands by prolonged vibration until the pigtail is cut through and left free to wave about, with possibilities for motor flashover or ground.

Dusty operating conditions allow granules of dirt to collect between brush and brush holder carbon way which results in causing the brush to stick so that the freedom of vertical motion is restricted, and the brush does not ride the commutator properly. This contributes to sparking, flashovers, rapid commutator, and brush wear. These granules leave unmistakable evidence in the side walls of the brush in the form of lightning-shaped threadlike grooves, radiating from the top to the bottom of the brush where it makes contact with the carbon way. The correction is to find where and how grit enters the equipment and then take preventive measures if possible, and to slide the brushes frequently in the holders and blow the clearance space clear of dirt.

The screw which secures spring lever shunt to brush holder body is worthy of careful inspection, being hard to get at and commonly overlooked, yet capable of serious damage to the motor. When this screw works loose and causes poor contact between the spring lever shunt and the brush holder body, the current normally carried by the shunt is caused to flow through the brush holder spring instead. Under heavy load, this current may be of sufficient magnitude to anneal the spring and cause loss of spring tension on the brush. The failure of the brush to maintain contact with the commutator causes arcing and overloading of other brushes, which ultimately leads to flashovers or complete destruction of the brush holder by melting off from sustained arcing. The possibility for this screw to work loose completely and fall onto the commutator where it jams against a brush holder and causes a deep groove to be cut into the commutator, short-circuiting all segments, is more serious, making it necessary to cut the motor out of service and remove it for major repairs.

COMMUTATORS REVEAL HISTORY OF LOCOMOTIVE HANDLING

Observation of commutator surface appearance is a quick means for sizing up the general health of the equipment, and poor appearance is evidence to the practiced eye that trouble exists, but it is not always so simple to ascertain quickly the true cause. Fortunately commutators are capable of long suffering, and like gearing they seldom fail suddenly but give ample warning of impending trouble in time to hunt down the cause. Poor commutator condition does not imply always that the cause is within the machine, but may be traceable also to external faults or conditions, the effects of which reveal themselves on the commutator and leave their record burned into its surface.

For instance, it is not uncommon for enginemen trained in steam practice to stall the locomotive completely, intentionally or forgetfully, for unduly long periods with partial power on the motors. This is harmless in steam and even good practice under some circumstances but is capable of extreme damage to the electric drive. This may occur when a train is braked to a stop with power on to keep the train slack stretched out for a smooth start and then power is not shut off for the duration of the stop. Again, a train may be held on an inclined spur with power on while the train crew look for blocks to hold a car so that it can be uncoupled from the train.

Fundamentally, any operation which brings a motor to standstill while heavy current continues to flow causes intense local heating at the point of contact between brushes and commutator, especially if brushes are tipped to contact along one edge of their face by a slight backward movement of the armature just before the full stop was made. These local contact spots when unduly prolonged may reach red or white heat and anneal the copper commutator segment involved and also burn the shellac binder in the mica segment or mica commutator cone.

This local damage, varying from one overheated bar to as many as four groups of bars spaced 90 degrees apart in severe cases, does not immediately incapacitate a motor. The overheated segment now annealed, or the burned mica, lacks the necessary strength to keep its position under the action of centrifugal force at high operating speed and creeps above the general commutator bar surface to become a high bar with the attendant marking and burning of the surface, or the shattering of brushes and frequent flashovers.

Such bars may be found near or in the telltale burned spots at equally spaced intervals 90 or 180 degrees apart, and it may be evident by the discoloration at their ends, marking them from the others, or they may require searching for with a Scleroscope or some equivalent means.

While it is possible to keep such motors in operation by frequent cleaning or stoning of the commutator surface, ultimately a major repair is necessary to remove the annealed bars and patch, or to renew mica cones and reassemble with new hard bars.

A closer regular pattern of burned bars, especially in generators, traceable to external causes, may be brought about by the engineman failing to make a backward transfer to the series connection upon encountering a grade with a heavy train, so that the speed is dragged down, and the current of motors in parallel connection exceeds the commutation ability of the generator and, when prolonged, burns the commutator and overheats the armature winding.

A similar commutator condition is also possible by the accumulative flashing at brushes over a period of time, brought about by a delayed transition from series to parallel motor connections, in which case the control equipment requires attention and adjustment.

Sluggish reverser operation during extremely cold weather, when air-compressor oil condensate may reach reverser air cylinders, can cause severe shock to the entire transmission apparatus by allowing Diesel-engine speed to rise well above idling speed before the circuit is closed through the reverser contacts.

Sluggish air brakes may lead to emergency or convenient plugging of motors into reverse while the train is still moving ahead to effect a quick stop. The magnitude of the commutation task under such conditions is bound to blast the commutator surface, leaving a wake of splattered copper beads and craters in the surface to upset the smooth riding of the brushes.

RESURFACING COMMUTATORS DURING LAYOVER TIME

There are occasions, whatever may be the cause, when locomotive commutators require resurfacing in order to continue operation. Much of this can be accomplished quickly and easily in the locomotive unless the condition is such as to justify removal to a repair shop.

Such resurfacing is commonly done with a commutator resurfacing stone held by hand or in a tool and applied to the commutator, rotating at sufficient speed to do a good grinding job. When the stone is hand held, it should be large enough to permit a maximum arc of contact with the surface. This will allow it to ride over the low spots, rather than down into these hollows, and thus cut the high spots down first and thereby tend to produce a cylindrical surface. Remove a brush holder when it prevents the use of an ample stone size.

When irregularities are beyond correction with a hand stone, a rigid tool or fixture can be adapted to the special needs of the machine to hold the cutting stone steady or to mount a conventional Carboloy-tipped lathe cutting tool for the rough cut; then one can finish with a stone.

The brushes should be removed during the resurfacing operation to prevent their rapid wear and dusting. Clean the oily and greasy surfaces before proceeding with the resurfacing so as to prevent adherence of the grinding dust and to permit easy cleaning of the equipment after finishing with the operation. When possible, erect cardboard shields so as to baffle the interior of the machine against entrance of this dust.

Install two old brushes to supply power for driving the motor armature during the stoning. The axle, mounting the motor, is then by suitable means jacked up so that the armature and wheels are free to rotate. The remaining wheels of the locomotive must be blocked or have the brakes set so as safely to hold the locomotive stationary while the one axle is raised and left free to rotate.

Driving power may be obtained simply from the locomotive's own Diesel-electric power plant by going into the first operating position which, in most cases, will provide sufficient speed to do an excellent resurfacing job. The current required to rotate the armature is so low that no damage will result to the stationary motors through which this same current is flowing. An alternative, if preferred, is to obtain power from a welding set or some other suitable d-c power source.

In the case of a generator a similar resurfacing operation is possible in the locotomotive in first starting and warming up the Diesel engine so as to permit a quick easy start later, then shutting down and removing most of the brushes from the commutator, leaving only a few easy-to-reach brushes in about three quarters of the readily accessible brush holders of each polarity.

The engine is again started, and these remaining brushes are removed, and the resurfacing operation is ready to proceed without affecting the brushes, which, as in the case of the motors, would be excessively worn during a prolonged resurfacing operation. Block out all contactors to insure no voltage during the process.

THE "AIR CURE"

Thorough cleaning of the apparatus is essential, and special attention must be given to the undercut spaces between segments to see that all dragged over copper, accumulated dirt, and carbon are removed.

A powerful tool for the detection and cleaning of these spaces is a procedure referred to as "air curing." A strong jet of dry compressed air is blown against the commutator, held close to, and directed across the surface, so that the impact will dislodge material in the groove while the commutator is rotated at gradually increasing speed and maximum-no-load voltage.

In the case of a generator, this is done with motor contactors blocked out to open the load circuit and field contactors closed in to obtain maximum excitation at full engine speed.

In the case of a motor, this is done at available voltage within the speed limitation of the motor, which for practical purposes is sufficient. It is possible, when necessary, to obtain full motor voltage at full speed by resorting to separately controlled motor-field excitation circuit in order to keep within the maximum motor-speed limitation.

When sparking or rings of fire appear under the impact of the air jet, the speed and voltage should be held until they clear up. It may, in stubborn cases, be necessary to shut down and dig the troublesome spots clean before proceeding.

It is well to apply this air-cure process to motors or generators reported to flash over repeatedly for unknown and unfound causes, as a means of locating weak spots in commutator segment insulation.

The operator should protect himself during resurfacing and air-cure processing against flying particles, dust, and flash-over by wearing goggles, respirator, and suitable gloves in keeping with approved safety practice.

CABLES, CLEATS, AND CONNECTIONS

Motor cables and cleats should be examined each trip as the swing of cables between locomotive and motor concentrate stress at the cable cleats which becomes the point of wear and cracks in insulation. Such defects, if not discovered and repaired, will lead to motor or power-plant failure, especially during wet weather when creepage over and through these broken surfaces will result in a ground which may be serious enough to burn off a motor cable.

Motors are at times removed in emergencies, and during the rush the contact surfaces of the terminals may be overlooked so as to make contact through a dirty surface which, later in operation, will cause overheating and the melting of the soldered connections between terminal and motor cable, which may become serious enough to burn the connection off completely, open-circuit, or ground the equipment.

Loose Bands Can Lose Armatures

The loss of a power plant may overload the remaining equipment in order to bring the train in. Operators may take pride in bringing a train in under such conditions and justly so, but the maintenance man must be on the alert to see what the consequences of such operation are to the remaining equipment. Enough damage may result when the equipment is heavily overloaded to cause loosening of bands, shrinking of insulation, and slot wedges which permit the vibration of armature windings. The mechanical vibration of conductors ultimately wears insulation thin so that final failure occurs by electrical breakdown when it is least expected and under comparatively easy operating conditions. Such failures result from former abuse.

Similar damage from overheating is caused by operation without traction motor blowers when they are necessary for adequate ventilation of the motors, or by insufficient air when the fan belts slip.

Overloaded motors should be changed out as soon as possible without waiting for the completion of their standard reconditioning mileage. Remove the armature immediately to make a careful inspection of bands, wedges, and insulation.

Extreme temperature gradient will exist within the motor during overload operation. In parallel ventilated traction motors the armature temperature may be expected to increase from the commutator end to the pinion end. The pinion end winding will be hottest, because it is insulated and covered over by a band and is ventilated by the hottest air, while the commutator end of the winding can conduct its heat to the exposed surface of the commutator. Where overloads persist as on a grade, or where they are repetitive, such as in frequent starting of a train with one of two power plants shut down, pinion end temperature easily may reach a value sufficient to melt the solder on the pinion-end band and yet leave the commutator-end band and commutator-rise soldering intact.

Such damage may easily escape detection by casual observation through the commutator-end inspection opening. Hence the importance of disassembling to be able to view, tap, and feel the pinion-end band for signs of loosening.

A band unsoldered, especially at speed, will cause its relaxation, and this along with shrinkage of varnish and insulation at these extreme temperatures permits vibratory movement of the windings. Prolonged operation with such mechanical motion wears through the insulation and culminates in ultimate electrical failure.

This mechanical condition detected in time has good prospects of successful repair if the band and band insulation are removed and the armature winding is thoroughly treated in good insulation varnish and again rebound. This treatment is comparatively quick and inexpensive as compared with a serious failure caused by neglect, necessitating a complete rewind in order to restore it to service. The prospect of successful operation, after delayed treatment, is difficult to predict. Slot wedges should be checked also and renewed when damaged, as these may allow the winding to burst out of the slots even though the end bands are renewed.

This is a good time to emphasize the importance of proper anchoring of the ends of the binding wire. The anchor should be such that it will hold even after the solder is thrown off. It will permit operation so as to bring the train in, which otherwise may have resulted in losing a band and bursting the winding.

There are various designs for anchoring of the band, the basis of which is to have the start and finish of the band caught in the same clip extending through from one side of the band to the other. The ends of the band are looped back and then secured by pulling the end of the clip back under the turns adjacent to the end. A band so secured may slip and relax slightly when unsoldered but will not let go.

When overloads are sustained so that all parts of the armature tend to reach uniformly high temperature, all bands will unsolder as well as the commutator-riser connections, and this is easily detected by observation through the commutator-end inspection opening. Operators are quick to remove such obviously damaged motors, but unfortunately where outright failure has not occurred, the tendency is to assume that all is in order and to continue operation, while under the surface of the relaxed armature band the vibrating winding is eating into the vitals of the armature.

Failures do not happen—they are caused, and they deserve close engineering inspection and analysis with unprejudiced zeal to search the events leading up to the failure before corrective measures in design, operation, or maintenance can be applied intelligently and effectively.

Synchronous Motors With Controlled Excitation for Suddenly Applied Loads

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It is common practice to operate synchronous motors with full-rated field current supplied from a constant-voltage exciter. In a number of cases involving variable loads, automatic control equipment has been used to vary the excitation with the load. This control causes the field current to be reduced below the rated value during light-load periods when it is not needed and increases the excitation when the load is applied, in order to prevent the motor from pulling out of step. By this means both operating efficiency and system voltage regulation have been improved.

Operating efficiency is improved by reduction of losses caused by motor field current and armature current. Field-current losses are reduced, because this current during light-load periods is less than the rated value. Armature current losses in the motor and in the power-supply system are also reduced, because the motor reactive kilovolt-amperes during light-load periods is less than that with full excitation applied.

System voltage regulation is improved, because the motor does not furnish unnecessary reactive kilovolt-amperes during light-load periods when it would tend to raise the voltage to an undesirably high value. Also, regulation is improved, because, as the load is increased, the leading reactive kilovolt-amperes furnished by the motor do not diminish and may even be increased, thus offsetting the tendency of the load to reduce the system voltage.

The advantages described have previously been recognized[1] in respect to motors whose loads vary gradually, so that plenty of time is available for increasing the excitation as the load increases. The advantages of use of automatic control for motors for loads which are applied suddenly has not been generally appreciated. This paper presents the results of an investigation of the use of synchronous motors with controlled excitation for suddenly applied loads. Quantitative results are given covering a range of machine and control characteristics and operating conditions. It was found that the advantages of improved operating efficiency and system voltage regulation can be obtained even when loads are applied suddenly. Also, when the peak loads are high relative to the required continuous load rating of the motor, certain additional advantages are available.

Suddenly applied loads with short-time peak values much greater than the average load are encountered in numerous applications for synchronous motors, such as for some metal rolling mills, rubber mills, and similar drives. In such cases the size of the motor is often determined principally by the peak load. If the motor is excited from a constant-voltage exciter, the field must be designed to carry continuously sufficient current for maintaining synchronism with peak load applied. If, however, the field need carry only the excitation required by the load, and this field current is automatically increased when peak load is applied, a smaller motor may often be used. It was found that with properly co-ordinated designs of motor and control, for a wide range of ratings and operating conditions, substantial savings in critical materials, principally copper and steel, will be obtained. For the example illustrated in Table I it was possible to employ a design of motor and control which resulted in an over-all saving of 2,000 pounds of copper and 12,000 pounds of steel. These savings are of particular importance at the present time of shortages of these materials.

Table I. Example of Synchronous-Motor Application Involving Suddenly Applied Loads.

Comparison of Two Motors, Each for 3,500-Kw Motor-Generator Sets for Reversing Blooming Mill

	Motor A	Motor B
Type of excitation supply	Automatically controlled	Constant voltage
Continuous horsepower rating	4,900 horsepower	4,900 horsepower
Power-factor rating	1.0 power factor	0.8 power factor* or 1.0 power factor
Exciter continuous rating	40 kw	40 kw
Maximum peak load	300 per cent	300 per cent
Approximate total weight of copper (motor and control)	4,000 pounds	6,000 pounds
Approximate total weight of steel (motor and control)	48,000 pounds	60,000 pounds
Operating efficiency (duty cycle, 100 per cent rms load, comprising repeated 250 per cent short-time peaks alternating with no-load periods)	93.1 per cent	91.1 per cent

* For motors with 300 per cent steady-state pull-out torque, the design requirements of 0.8 power factor and 1.0 power factor motors are approximately the same.

In the case outlined, it was found that the addition of the excitation control equipment did not result in an increase in over-all cost. For ratings considerably

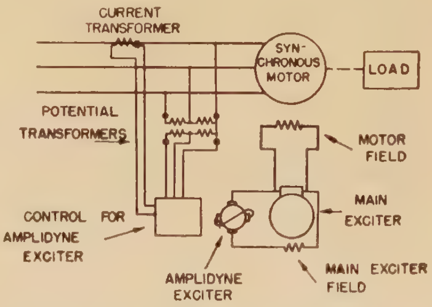

Figure 1. Circuit diagram, synchronous motor with controlled excitation for suddenly applied loads

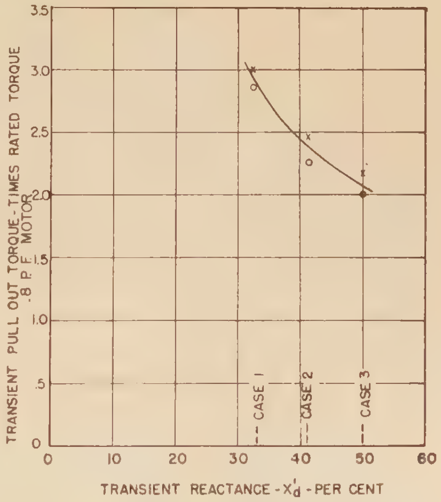

Figure 2. Effect of transient reactance on transient pull-out torque

Conditions of cases 1, 2, and 3

x—Motor pulls out with this value of suddenly applied torque

o—Motor remains in step with this value of suddenly applied torque

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smaller than that of the example given, the over-all cost is slightly higher than for comparable motors and control of conventional design.

It was found that the use of controlled excitation makes possible motor designs which for many cases have lower values of full-voltage starting current than comparable conventional designs. When full-voltage starting is to be used, this feature will reduce the disturbance to the system caused by starting the motor.

It is emphasized that a completely co-ordinated design of motor and control is necessary to assure satisfactory performance and to make available the advantages described. In some cases, the design of the motor will be determined by requirements of the continuous rating, reactive kilovolt-amperes for power-factor correction, starting torque, or pull-in torque, as well as the peak load. The influence of these other factors should be evaluated in each of such cases.

A typical excitation control system is illustrated by Figure 1. The automatically controlled amplidyne exciter varies the motor excitation by varying the voltage applied to the field of the main exciter. The control adjusts motor excitation to load requirements by responding to changes in motor input.

Effects of Design Factors

In this paper, the value of suddenly applied torque just sufficient to cause pull-out is called the transient pull-out torque. By calculating motor performance for a variety of machine constants and operating conditions, their effects on the value of this torque were determined. These effects are illustrated by Figures 2, 3, 4, and 5. For purposes of comparison, Figure 6 is included, showing the steady-state pull-out torque. This figure applies for all the cases investigated. Figures 2 to 6 are all plotted in terms of the rated torque and rated field current of an 0.8 power-factor motor with a steady-state pull-out torque as given by

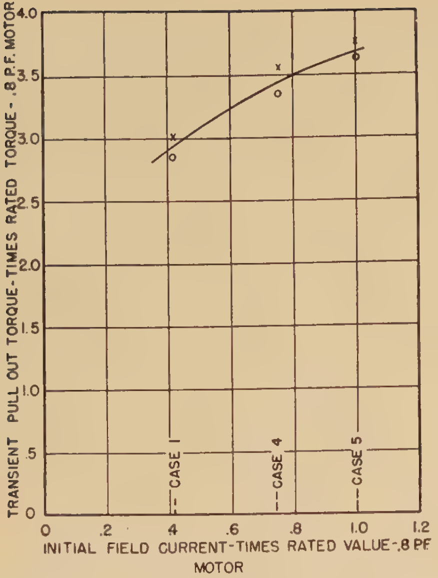

Figure 3. Effect of initial field current on transient pull-out torque

Conditions of cases 1, 4, and 5

x—Motor pulls out with this value of suddenly applied torque

o—Motor remains in step with this value of suddenly applied torque

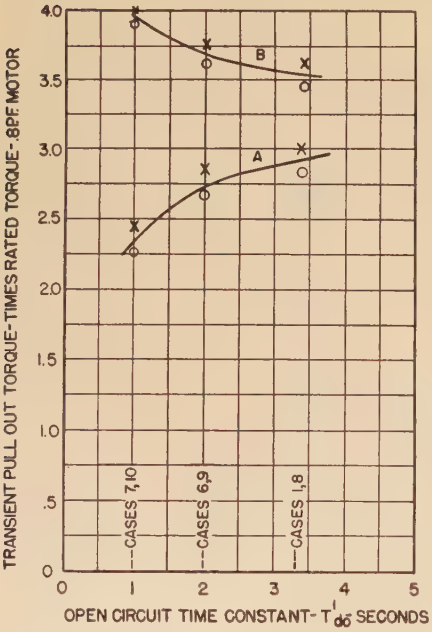

Figure 4. Effect of open-circuit time constant on transient pull-out torque

x—Motor pulls out with this value of suddenly applied torque

o—Motor remains in step with this value of suddenly applied torque

A—Conditions of cases 1, 6, and 7. Field voltage rises at rate of 110 per cent of rated field voltage per second to ceiling value of 180 per cent

B—Conditions of cases 8, 9, and 10. Field voltage rises instantly to ceiling value of 300 per cent of rated field voltage

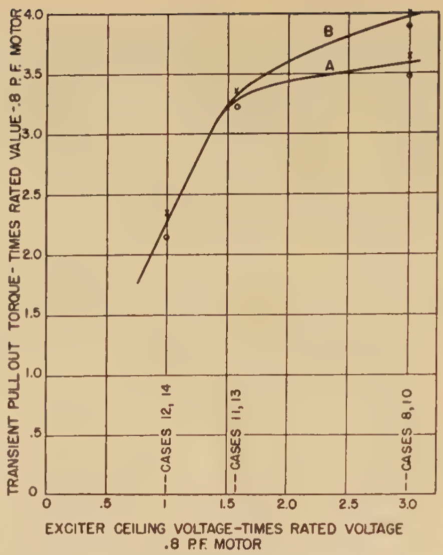

Figure 5. Effect of exciter ceiling voltage on transient pull-out torque

x—Motor pulls out with this value of suddenly applied torque

o—Motor remains in step with this value of suddenly applied torque

A—Conditions of cases 8, 13, and 14—$T_{do}' = 3.42$ seconds

B—Conditions of cases 10, 11, and 12—$T_{do}' = 1.0$ second

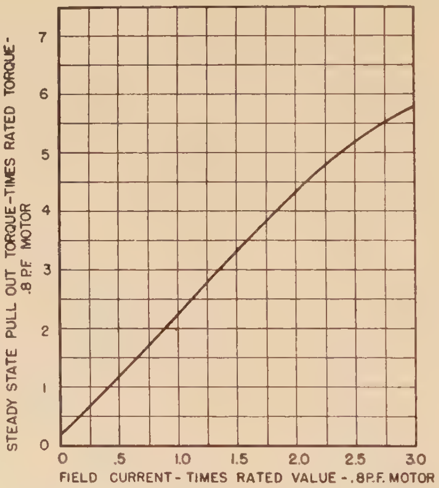

Figure 6. Steady-state pull-out torque

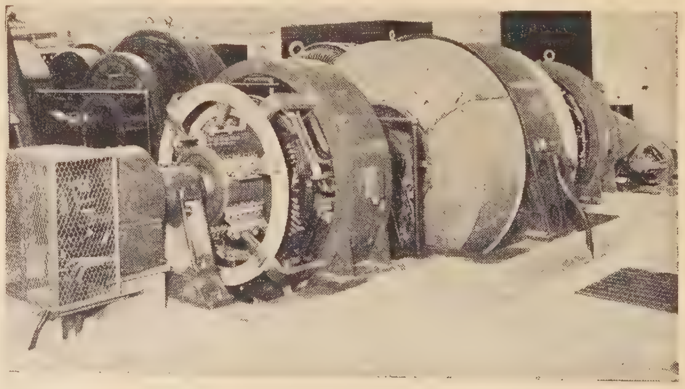

Figure 7. Motor-generator set used for tests

Motor rating—2,100 horsepower, 2,100 kva, 0.8 power factor, 900 rpm, 13,800 volts, three phase, 60 cycles

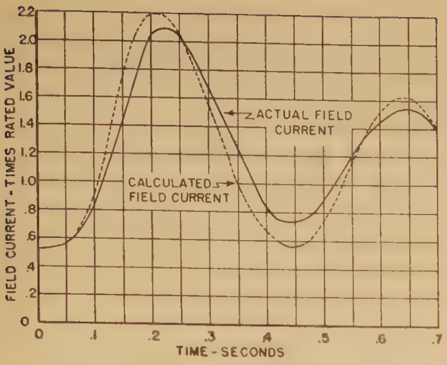

Figure 8. Comparison of calculated field current with test results

Motor rated 2,100 horsepower, 2,100 kva, 0.8 power factor, 900 rpm, 13,800 volts, three phase, 60 cycles

270 per cent load suddenly applied

Figure 6. The results can be interpreted, however, as applying to motors with other power-factor ratings and steady-state pull-out torque characteristics, by properly modifying the scales for the curves. The methods of modifying them are given in the appendixes.

Figure 2 shows the effect of transient reactance on transient pull-out torque. The maximum suddenly applied torque which can be carried is seen to vary almost in inverse proportion to the value of transient reactance.

Figure 3 shows the effect of initial field current. While decreases in the current lower the transient pull-out torque, it is seen that this initial current may be well below rated value without greatly reducing the maximum safe torque.

Figure 4 shows the effect of open-circuit time constant for two types of exciter voltage changes. Curve *A* corresponds to an excitation voltage which, as soon as load is applied, rises at a constant rate of 110 per cent of rated field voltage per second toward a maximum or ceiling voltage of 180 per cent of rated field voltage. The equivalent of this type of response is obtainable by means of the type of excitation system illustrated by Figure 1. Curve *B* corresponds to an excitation voltage which rises instantly, upon the application of load, to a ceiling value of 300 per cent of rated field voltage. This more rapid type of response is obtainable only by a regulating system with exceptionally high speed characteristics such as are obtainable by use of an electronic main exciter. It may be seen that increases in the time constant are helpful when the excitation system is of the type shown by Figure 1, but that increases in the time constant decrease the transient pull-out torque when an electronic exciter with a relatively high ceiling voltage is used. Figure 4 shows that for machines with the longer time constants, the transient pull-out torque available with an excitation control of the type shown by Figure 1 approaches closely to the torque available with an electronic exciter with a high ceiling voltage. For the lower values of time constant, corresponding to relatively smaller motors, the advantages of an electronic main exciter or its equivalent are more pronounced.

Figure 5 shows the effect of ceiling voltage on transient pull-out torque. The curves of Figure 5 apply to use of an excitation system which raises the field voltage immediately to the ceiling value.

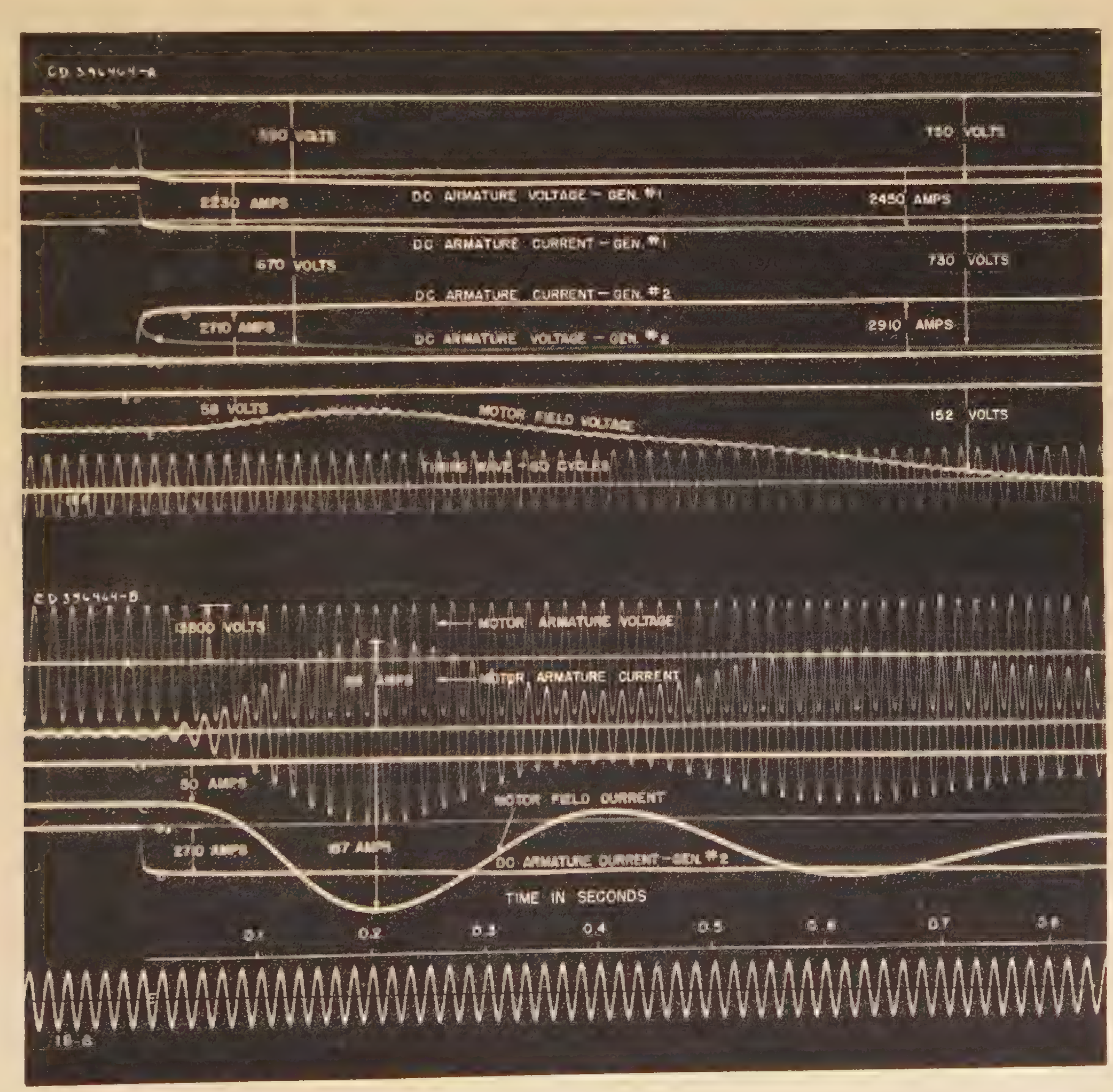

Figure 9. Oscillograms of test of synchronous motor with controlled excitation for suddenly applied loads

These curves are plotted for two different values of time constant. The curves show that there is relatively little to be gained by employing very high ceiling voltages. When the field voltage does not rise immediately to the maximum value, in some cases there will be nothing gained by use of a high ceiling voltage, because the motor will definitely pull out or will definitely reach stable synchronous operation before the exciter reaches its ceiling.

The effects of design constants and operating conditions other than those investigated for this paper may be determined readily by the method of analysis outlined in the appendixes.

Test Results

To check the reliability of the analysis and calculations of this paper, tests were made on the 2,100-horsepower 900 rpm 13,800-volt, 0.8 power-factor motor driving the motor-generator set shown by Figure 7. The pull-out torque of this set, with rated excitation applied, is 220 per cent. An excitation system of the type shown by Figure 1 was used and adjusted to supply at no load a field current of approximately 50 per cent of rated excitation. A load of 270 per cent of rated load was suddenly applied to the motor by connecting the two d-c generators of the set to a water box load. The motor did not pull out of step. Figure 8 shows a comparison of calculated field current with measured field current. Further tests were made in which the transient reactance of the motor was increased by adding external inductance to the field circuit of the machine. By successive trial applications of load, the load just sufficient to cause pull out was found. This load corresponded closely to the transient pull-out torque as determined by calculations. Figure 9 shows a typical oscillogram taken during the tests.

Conclusions

Following is a summary of the conclusions reached:

1. For a wide range of ratings of motors for loads with relatively high, suddenly applied short-time peaks, use of suitable automatic excitation control offers, without increasing over-all initial cost, the following advantages over use of motors excited by constant-voltage exciters:

(*a*). Savings in critical materials, particularly copper and steel.

(*b*). Increase in over-all operating efficiency.

(*c*). Improvement of supply-system voltage regulation.

(*d*). Reduction of full-voltage starting current.

2. Properly designed excitation control systems operating in exciter field circuits are adequate for the majority of cases in the range investigated.

3. The transient pull-out torque will be increased by reducing machine reactances, particularly the direct-axis transient reactance.

4. The transient pull-out torque will be increased by increases in the following:

(*a*). Initial field current.

(*b*). Rate of rise of field voltage caused by action of excitation control.

(*c*). Exciter ceiling voltage, in some cases.

5. The transient pull-out torque available when a suitable excitation system is used, is in most cases greater than the steady-state pull-out torque available with the field current for rated continous load.

Appendix A. Assumptions

The following assumptions were used:

1. The direct- and quadrature-axis armature reaction magnetomotive forces are proportional to reactive voltage drops equal to $i_d(X_d-X_l)$ and $i_q(X_q-X_l)$, respectively.

2. The field current, I_s, required to overcome saturation is a function only of the total direct-axis air-gap flux and the field current.

3. The value of I_s may be determined by the relationships of Figure 14 and the assumed empirical relation, $e_s=e_{ld}+0.12I_{fd}$.

4. The effects of amortisseur windings and other rotor circuits in addition to the field winding are negligible except in producing a damping torque proportional to the rate of change of rotor angle.

5. Armature resistance is negligible.

6. Impedance in the power system supplying the motor is negligible.

Appendix B. Nomenclature

The following nomenclature is used in this paper:

e_t = terminal voltage per unit
e_{ld} = direct-axis component of air-gap voltage
$e_d{}'$ = direct-axis transient internal voltage
e_s = equivalent voltage determining saturation requirements. $(e_s=e_{ld}+0.12I_{fd})$
i = armature current
i_d = direct-axis component of armature current
i_q = quadrature-axis component of armature current
I = total field current
I_{qd} = field-current requirement based on quadrature-axis synchronous reactance
I_{fd} = field-current requirement excepting effect of saturation
I_s = field current required to overcome saturation
X_d = direct-axis synchronous reactance
X_q = quadrature-axis synchronous reactance
$X_d{}'$ = direct-axis transient reactance
X_l = leakage reactance
δ = angle in electrical degrees between e_t and the direct axis
D = damping-constant, torque per radian of angular displacement per radian of time
H = inertia constant, seconds; H

$$=\frac{0.231(WR^2)(\text{rpm})^2\times10^{-6}}{(\text{Base kva})}$$

$T_{do}{}'$ = open-circuit time constant, seconds
t = time in seconds
Δ indicates change in quantity during time interval, Δt
f = frequency, cycles per second
v = angular rate of change of δ, degrees per second
P_e = electrical torque output of motor
P_L = applied load torque
P_D = damping torque
P = net decelerating torque

Numbered subscripts refer to step numbers in step-by-step calculation.

Armature currents, armature voltages, reactances, torques, and inertia and damping constants are expressed in per unit on motor kilovolt-ampere, voltage, speed and frequency rating. On some figures, torque values are expressed in times rated torque of an 0.8 power-factor motor. For these figures it is assumed that the rated horsepower equals rated kilovolt-ampere. For motors (of any power-factor rating) having a ratio of rated horsepower to rated kilovolt-amperes other than unity, the torque scales of these figures should be divided by this ratio. Field currents and voltages are expressed in per unit of the value required for no load, rated voltage on the air-gap line. On some figures, field-current values are expressed in times rated current for an 0.8 power-factor motor. For these figures it is assumed that the rated current is 2.7 per unit. For any motor (of any power-factor rating) whose rated field current has a different per-unit value, the field-current scales of these figures should be multiplied by 2.7 and divided by the per-unit value of rated field current.

Appendix C. Method of Analysis

Approach to Problem

To determine whether or not a motor would remain in synchronism after application of a particular load under a particular set of conditions, a calculation was made resulting in a plot of rotor angle versus time. If this angle (by which the rotor axis is out of phase with the line voltage) does not exceed 180 degrees and approaches a constant value less than 90 degrees, it was concluded that the motor would not pull out.

To determine the transient pull-out torque of a particular machine under a particular set of conditions, the calculation just mentioned was made for several different values of applied load. By this means, two load values were found, one only slightly greater than the other, the larger of which causes pull-out and the lesser of which does not. The transient pull-out torque lies between these two values.

The effects of variation of machine constants and of excitation system performance were studied by determining the transient pull-out torques for various combinations of machine constants and operating conditions

Table II. Schedule of Cases

Case	Initial Load	Initial Field Current Per Cent of Rated*	Field-Voltage Response Per Cent of Rated* Per Second	Final Field Voltage Per Cent of Rated*	Synchronous Reactance X_d	Quadrature Reactance X_q	Leakage Reactance X_l	Transient Reactance X_d'	Damping Constant D	Inertia Constant H	Frequency f Cycles Per Second	Open-Circuit Time Constant (Seconds) T_{d0}'	Saturation Characteristics
1	0	41.5	110	180	1.63	1.00	0.16	0.33	10	1.79	60	3.42	Figs. 13, 14
2	0	41.5	110	180	1.63	1.00	0.16	0.415	10	1.79	60	3.42	Figs. 13, 14
3	0	41.5	110	180	1.63	1.00	0.16	0.50	10	1.79	60	3.42	Figs. 13, 14
4	0	74	110	180	1.63	1.00	0.16	0.33	10	1.79	60	3.42	Figs. 13, 14
5	0	100	110	180	1.63	1.00	0.16	0.33	10	1.79	60	3.42	Figs. 13, 14
6	0	41.5	110	180	1.63	1.00	0.16	0.33	10	1.79	60	2.0	Figs. 13, 14
7	0	41.5	110	180	1.63	1.00	0.16	0.33	10	1.79	60	1.0	Figs. 13, 14
8	0	41.5	Infinite	300	1.63	1.00	0.16	0.33	10	1.79	60	3.42	Figs. 13, 14
9	0	41.5	Infinite	300	1.63	1.00	0.16	0.33	10	1.79	60	2.0	Figs. 13, 14
10	0	41.5	Infinite	300	1.63	1.00	0.16	0.33	10	1.79	60	1.0	Figs. 13, 14
11	0	41.5	Infinite	160	1.63	1.00	0.16	0.33	10	1.79	60	1.0	Figs. 13, 14
12	0	41.5	Infinite	100	1.63	1.00	0.16	0.33	10	1.79	60	1.0	Figs. 13, 14
13	0	41.5	Infinite	160	1.63	1.00	0.16	0.33	10	1.79	60	3.42	Figs. 13, 14
14	0	41.5	Infinite	100	1.63	1.00	0.16	0.33	10	1.79	60	3.42	Figs. 13, 14

* −0.8 power-factor motor (rated field voltage and current assumed = 2.7 per unit).

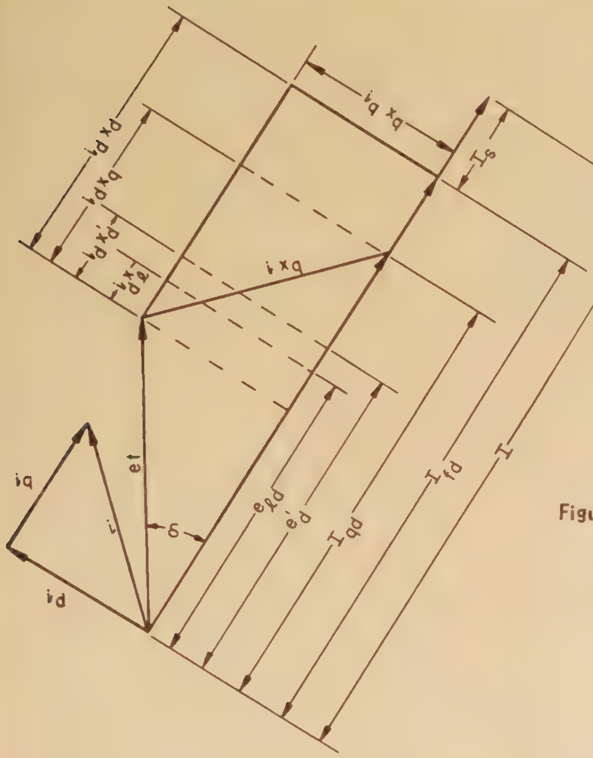

Figure 10. Synchronous-motor vector diagram

A schedule of cases studied is given by Table II. From the results of the various cases investigated, the effects of design factors were determined and plotted on Figures 2, 3, 4, and 5.

It may be observed from the form of the equivalent circuits that the pull-out power is increased in exact proportion to reduction in machine reactances if the ratios between these reactances is held constant. Therefore, all calculations were made with a single conservatively high value of direct-axis synchronous reactance, and it was not necessary to investigate separately the effect of changes in this reactance.

Method of Calculation

On the basis of the assumptions listed in Appendix *A*, Figure 10 may be drawn to represent the motor at all times, before, during, and after application of load.[2,4] Figure 11 corresponds to that portion of Figure 10 which is independent of saturation and of saliency of the poles of the rotor. The circuit of Figure 11 was set up on the a-c network analyzer,[3] illustrated in Figure 12. Reactance values representative of synchronous motors were used for the circuit of Figure 11. Readings of e_{ld}, e_d', and P_e were obtained for a range of values of δ and I_{qd}. Using these readings and the relationships given on Figures 10 and 14, corresponding values of I, which took pole saliency and saturation into account, were calculated. From the analyzer readings and the calculated values of I, curves such as those of Figures 15 and 16 were plotted. These were used in making step-by-step calculations of δ and I versus time. A sample step-by-step calculation is given by Table III.

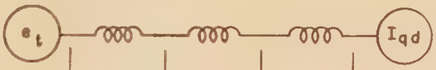

Figure 11. Circuit used on a-c network analyzer

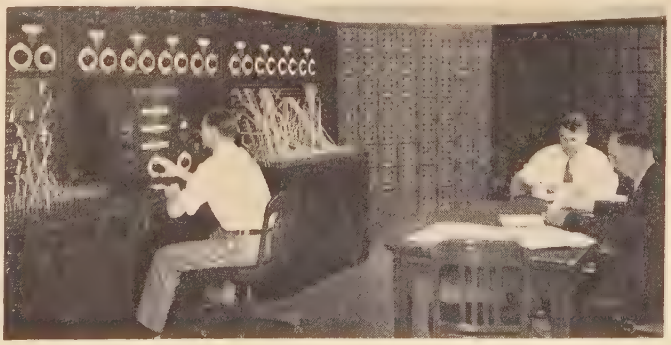

Figure 12. A-c network analyzer used for study of synchronous motors with automatic excitation control

The calculations of Table III proceed from left to right for each time interval, using last-calculated values of quantities required for making each calculation.

A time interval of 0.05 second was used for all calculations, since this was found by trial to give satisfactory accuracy. Use of a smaller interval will not materially increase accuracy.

The quantities given in Table III were calculated as follows:

The initial conditions were recorded for $t = -0.05$ second.

By definition

$$\delta_1 = \delta_0 + \Delta\delta_0 \text{ etc.} \tag{1}$$

and

$$e_{dl}' = e_{d0}' + \Delta e_{d0}' \text{ etc.} \tag{2}$$

I is determined from Figure 15 for the previously calculated values of δ and e_d'.

P_e is determined from Figure 16 for the previously calculated values of δ and I.

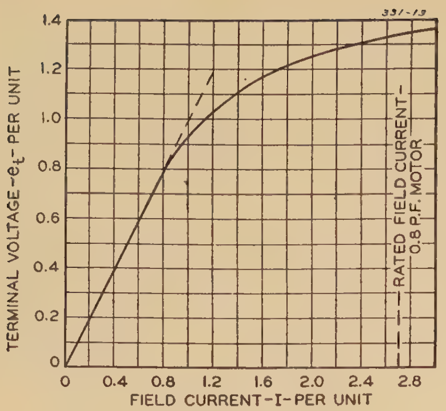

Figure 13. No-load saturation curve

P_L is the assumed applied load for the case studied.

From assumption 4

$$P_D=\frac{D}{360(f)(\Delta t)}(\Delta v)(\Delta t) \tag{3}$$

Since the sum of all torques on the rotor is zero

$$P=P_L-P_e-P_D \tag{4}$$

Since the decelerating torque

$$P=\frac{2H}{360f}\times\frac{dv}{dt} \tag{5}$$

$$(\Delta v)\,(\Delta t)=\frac{360f(\Delta t)^2}{2H}P \tag{6}$$

except for the first interval, for which from equation 55 of Reference 4,

$$(\Delta v_1)(\Delta t)=\frac{360f(\Delta t)^2}{4H}P_1 \tag{7}$$

By definition

$$\Delta\delta=(v_1)(\Delta t)=(v_0)(\Delta t)+(\Delta v_0)(\Delta t)\text{ etc.} \tag{8}$$

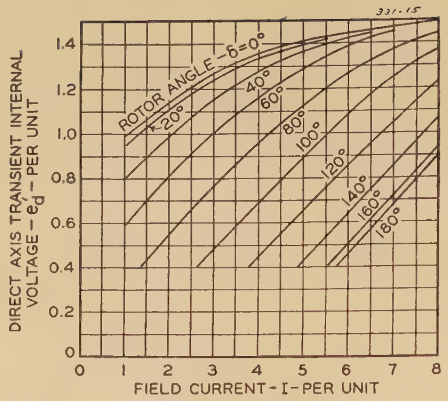

Figure 15. Direct-axis transient internal voltage, e_d'

$X_d=1.63 \quad X_q=1.00$
$X_d'=0.33 \quad X_l=0.16$
Saturation per Figure 14

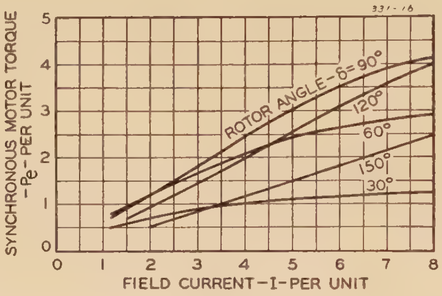

Figure 16. Synchronous-motor electrical torque

$X_d=1.63 \quad X_q=1.00 \quad X_l=0.16$
Saturation per Figure 14

Table III. Sample Step-By-Step Calculation—Case 1*A*

Suddenly Applied Load, $P_L=2.25$ Per Unit

Step	t	δ	e_d'	I	P_e	P_L	P_D	P	(Δv)(Δt)	Δδ	E	E-I	$\Delta e_d'$
0	−0.05	0	1.00	1.12	0	0	0	0	0	0	1.12	0	0
1	0.00	0	1.00	1.12	0	2.25	0	2.25	17.0	17.0	1.12	0	0
2	0.05	17.0	1.00	1.28	0.32	2.25	0.16	1.77	26.7	43.7	1.27	−0.01	−0.00
3	0.10	60.7	1.00	3.00	1.69	2.25	0.41	0.15	2.3	46.0	1.42	−1.58	−0.02
4	0.15	106.7	0.98	5.85	3.33	2.25	0.43	−1.51	−22.8	23.2	1.57	−4.28	−0.06
5	0.20	129.9											

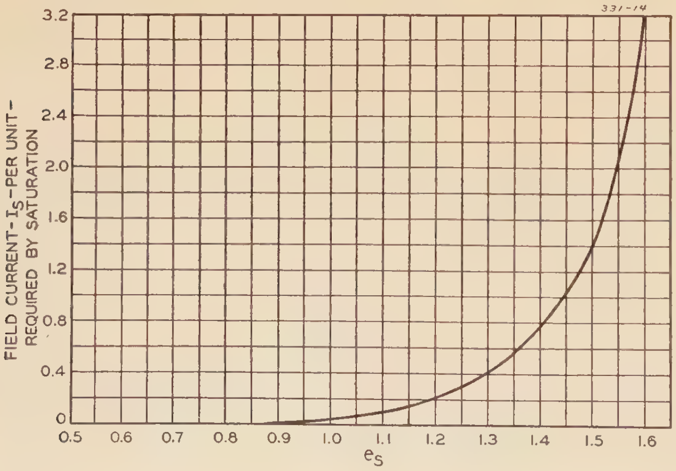

Figure 14. Field current required by saturation

$e_s=e_{ld}+0.12I_{fd}$
$I=I_{fd}+I_s$

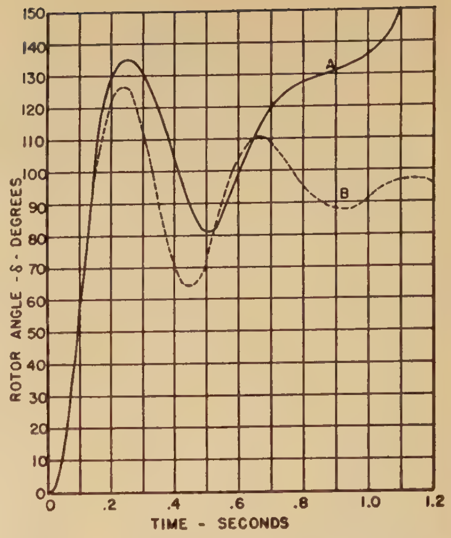

Figure 17. Calculated performance of synchronous motor—rotor angle versus time—case 1

A—Suddenly applied torque = 300 per cent of motor rated torque — 0.8 power-factor motor (motor pulls out of step)

B—Suddenly applied torque = 285 per cent of motor rated torque — 0.8 power-factor motor (motor remains in step)

E is as assumed for the case being studied.

From the second equation following equation 4a of Reference 5,

$$\Delta e_d'=\frac{\Delta t}{T_{d0}'}(E-I) \tag{9}$$

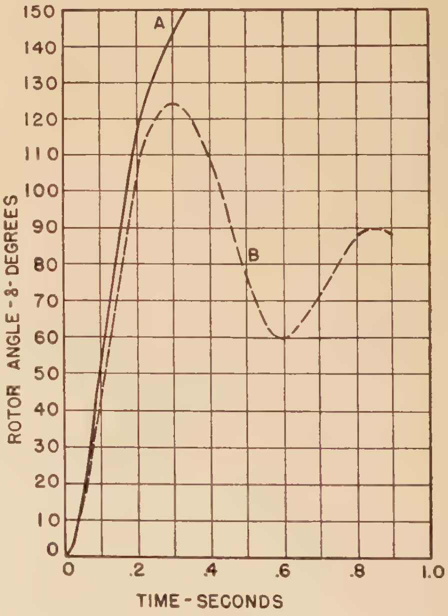

Figure 18. Calculated performance of synchronous motor—rotor angle versus time—case 3

A—Suddenly applied torque = 220 per cent of motor rated torque — 0.8 power-factor motor (motor pulls out of step)

B—Suddenly applied torque = 200 per cent of motor rated torque — 0.8 power-factor motor (motor remains in step)

A Multiorifice Interrupter for High-Voltage Oil Circuit Breakers

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Synopsis: High-voltage oil circuit breakers for high-speed operation generally use some form of multibreak contact arrangement. These breakers perform satisfactorily but lack the simplicity of the conventional slower-speed two-break construction. The development of an unusually effective multiorifice oil-flow interrupting unit now makes it possible to reduce the required number of breaks to the point where even three-cycle 230-kv breakers appear practical with only two interrupting units per pole.

High-power laboratory tests on low magnetizing and charging currents, as well as short-circuit currents corresponding to interrupting ratings of 2,500,000 kva and above, have demonstrated the exceptional arc-extinguishing effectiveness and low arc-energy dissipation of this multiorifice interrupter for high-speed oil circuit breakers.

PRESENT-DAY requirements relating to the manufacture and operation of high-voltage oil circuit breakers demand the least use of critical materials and the minimum time out of service for inspection and maintenance. Modern circuit-breaker designs, therefore, must have the highest possible interrupting efficiency in order to limit the arc-energy dissipation during circuit opening with attendant deterioration of oil, contacts, and interrupter parts. Since physical size and material usage as well as maintenance depend on interrupting efficiency, the designer must give primary consideration to an improved interrupter in order to achieve any essential simplification of manufacture and operating procedure.

In the voltage range from 115 kv to 230 kv, most oil breakers for eight-cycle operation and some for five-cycle time are constructed with two interrupters in a pole unit.[1,2] The mechanical arrangement is simple, and the potential advantages associated with an improved interrupter are to be gained therefore from decreased energy release, which leads to decreased size and improved performance. For three-cycle operation at the higher voltages, the structure becomes necessarily more complicated because higher operating speeds are required, and in general it has been necessary to utilize a multiplicity of interrupting devices and contacts in each pole unit.[3]

While the multibreak design has shown satisfactory performance, a comparison between such contacts and the simpler structures used in the conventional two-break assemblies for eight-cycle operation suggests the desirability of reversing this trend toward a large number of breaks. By making use of newly developed and highly effective oil-flow interrupting units, it appears practical to build even the highest-speed breakers with but two interrupting units per pole.

An interrupting function of the high-voltage high-speed circuit breaker is the interruption of line-charging current when little or no load is being carried. Operators have expressed concern over the possibility of producing high destructive overvoltages as a result of restriking of the arc within the breaker during such operation. Particular attention has been paid to the possibility, and test results with a new interrupter are shown which minimize restriking by prompt effective interruption of low currents.

The Evolution of a New Interrupter

The problem of high kilovolt-ampere interruption under oil has always been one of achieving a sufficient rate of deionization following a current zero, so that a high-voltage gradient can be quickly withstood across the open contacts with the minimum amount of energy dissipated in the arc prior to the current zero. Obviously, if means can be found to increase the volts per inch which can be interrupted, then a comparatively simple structure employing only two interrupters per pole unit is possible, even with the short contact separation which can be achieved in three cycles. The minimum mechanical time required from energizing the trip coil until the contacts part on a three-cycle breaker is approximately $1^1/_2$ cycles. This means that the maximum contact separation must be achieved in $1^1/_2$ cycles, and with reasonable mechanical speeds the contacts can be parted approximately three inches in this time. On the basis of two interrupters for 230 kv, and assuming line to ground of 132 kv across a pole unit, the interrupting voltage gradient to be achieved therefore must be in excess of 22,000 volts per inch.

The interrupting ability of an oil circuit breaker has been attributed to the turbulence produced within the gas bubble formed by the arc when the contacts are separated.[4] Such turbulence requires continuous formation of gas from the oil, and intimate association between the arc and the walls of oil which define the bubble is thus required. In the plain-break circuit breaker proximity between the arc and the oil walls is not readily achieved, since the pressure generated within the bubble quickly drives the oil away from the arc core. The interrupting ability of the plain break has been greatly improved by enclosing it in various forms of arc-control devices. These devices limit the extent to which gases may expand within the oil bubble, and therefore the pressure within it is increased to such an extent that intimate contact between the arc and the oil is achieved. These pressures, however, also increase the arc voltage and the energy dissipated.

It has been found that the interruption can be improved much by flowing oil past the arc by separately applying pressure. This flowing oil stream has the beneficial effects of supplying cool oil and fresh un-ionized gas to the arc core, and of tearing bubbles of spent ionized gas away from the region surrounding the core. Thus the size of the gas bubble in which the arc plays can be limited without developing excess pressures within it, and the desired proximity of oil and arc

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Results of typical step-by-step calculations are shown on Figures 17 and 18.

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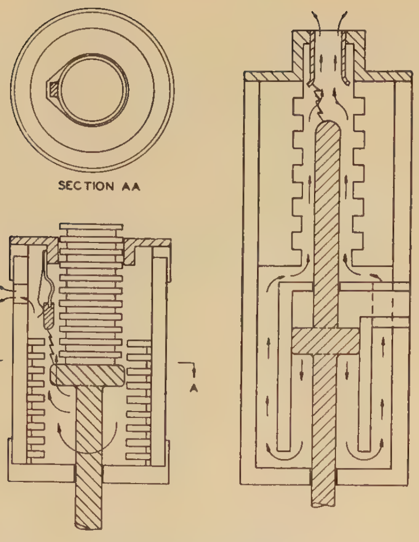


Figure 1. Oil-flow interrupters for high-voltage oil circuit breakers

Left—Vertical-flow interrupter
Right—Center-flow interrupter

can be achieved without high arc energy. It has been found also that, if the oil is caused to flow vertically along the axis of the arc, the disruptive effect of the oil stream itself on the arc core will be minimized. The arc in this case is not lengthened or bent about barrier plates; its voltage drop and energy loss are therefore low. Nevertheless, at current zero the gases formed from the decomposed oil are highly turbulent, and deionization is exceptionally effective[5]

B. P. Baker has previously described a vertical-flow interrupter and its application to oil-poor circuit breakers.[6] This interrupter consisted of a novel structure for developing an oil flow by mechanical means, the oil being directed longitudinally along the arc in a restricted channel vented at one end only, as shown in Figure 1a. This device was remarkable for the low arc voltage, and therefore low arc energy and oil depreciation, associated with the interruption of high short-circuit powers exceeding 2,000,000 kva. This device was tested both in the laboratory and in the field.

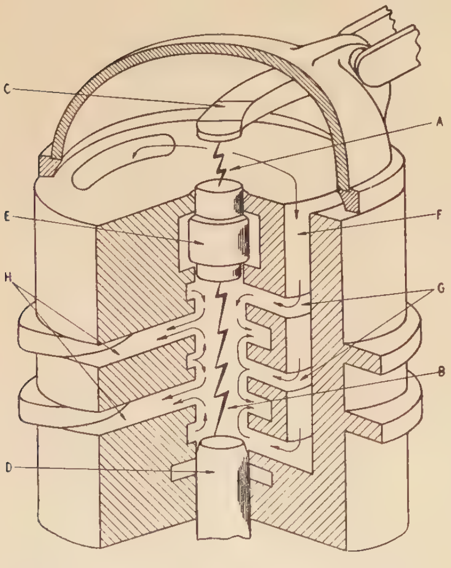


Figure 2. Cutaway view showing oil flow in multiorifice grid interrupter

A—Pressure break
B—Interrupting break
C—Upper contact
D—Lower contact
E—Intermediate contact
F—Oil-flow channel
G—Inlet passages
H—Exhaust passages

Experimental tests were made also on a center-flow device in which a contact was drawn down through a succession of orifices, while a flow of oil was driven longitudinally around the contacts and up along the arc through the orifices and out a vent at the top, as shown in Figure 1b.

Experience with these interrupters indicated that a high-voltage gradient could be developed only over a limited axial distance, because the oil stream soon became contaminated with ionized gas and was therefore not effective throughout the entire length of the interrupter. This phenomenon suggested that the effectiveness of the device can be greatly enhanced by building it in the form of a multiplicity of orifices through which the arc is drawn and through which the oil is caused to flow in an axial direction. If a fresh supply of oil is directed to each orifice, and if the used gases and oil are vented at each orifice, then the high-voltage gradient can be maintained in any desired number of inches. Consequently, the structure shown in Figure 2 was constructed. In such a structure the arc is not unduly disturbed prior to current zero, and its length is kept at a minimum. The pressures are definitely limited by the comparatively large open venting arrangement, and at the same time the deionization at current zero is extremely effective.

The oil flow can be produced in a number of ways, for example, by means of a piston which provides the necessary pressure. It is also feasible to utilize a second arc in the same interrupting structure which generates gas and thus forms sufficient driving pressure to cause the oil flow through the orifices and the main interrupting arc. This means has the advantages of producing a greater flow of oil as the current to be interrupted is increased. It also does not increase the mechanical load as with an oil piston. Consequently, the second pressure arc has been adopted in conjunction with the multiple-orifice structure and provides a simple small and efficient over-all interrupter as shown in Figure 5.

Description of the Multiorifice Interrupter

The cutaway view of the multiorifice interrupting unit shown in Figure 2 illustrates the essential features of the device. For maximum speed of arc rupture, a pressure break (*A*) and an interrupting break (*B*) are opened simultaneously by raising an upper contact (*C*) and drawing down a moving contact (*D*) away from a relatively stationary intermediate contact (*E*). Gas generation by the upper arc places oil under pressure and causes it to flow through a pair of oil-flow channels (*F*) to three pairs of inlet passages (*G*). Two pairs of exhaust passages (*H*) are located between the inlets, all channels being separated by fiber plates with a central orifice (*J*) in which the arc is drawn.

The arrows indicate the flow of oil which serves to keep the arc centered in the orifices, sweeping the arc products out of the vents and producing an extremely high rate of deionization of the

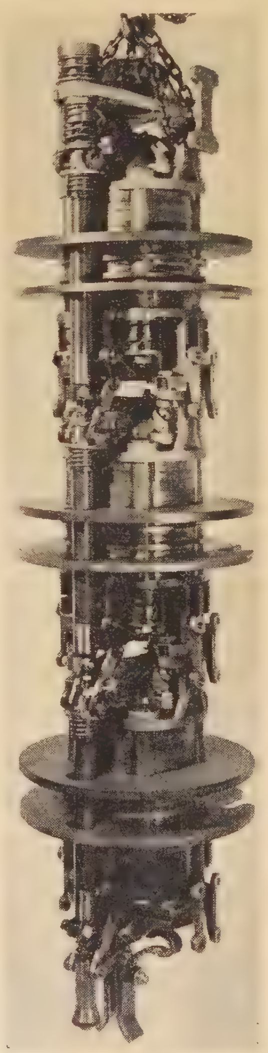


Figure 3. 287-kv three-cycle multiorifice grid-contact assembly with shields removed

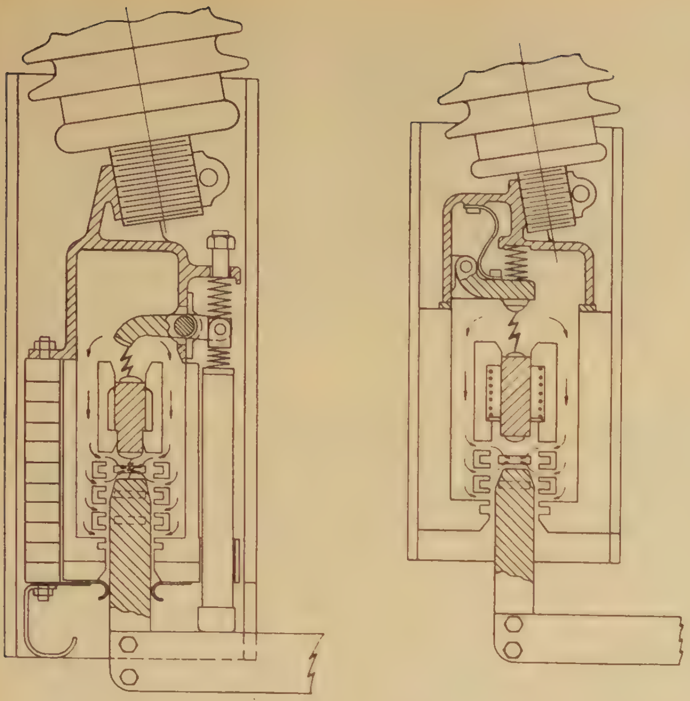
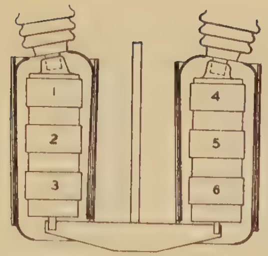


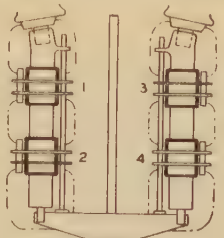
Figure 4. Multiorifice grids for 230-kv five-cycle breaker and 138-kv eight-cycle breaker

Left 230-kv five-cycle breaker multiorifice-grid assembly

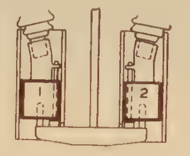
Right—138-kv eight-cycle breaker multiorifice-grid assembly



(a). Old design with six radial field grids



(b). Design with four multiorifice grids



(c). New design with two multiorifice grids

Figure 6. Evolution of 230-kv three-cycle breaker designs for 2,500,000 kva

arc space at current zero. The design lends itself naturally to the selection of more or less inlet–orifice–outlet units according to the voltage rating. Since the oil-flow passages are arranged in parallel, the quantity of oil to be set in motion by the driving pressure will be greater for increased voltage ratings, and a longer pressure arc is provided to overcome the increased pressure drop in the vertical oil-flow channels.

For maximum speed and effectiveness of arc rupture, the pressure break and main interrupting break are opened practically simultaneously as shown in Figure 4a, although satisfactory slower-speed performance is obtained with a consecutive sequence of contact parting as shown in Figure 4b. With the arrangement of Figure 4a, five-cycle operation is obtained with normal contact speed sufficient to give adequate contact separation in $2^{1}/_{2}$ cycles. In order to obtain three-cycle operation, either four grids per pole unit may be used, or the contact speed may be increased to give longer separation in $1^{1}/_{2}$ cycles, and two grids may be used. In this case heavier construction must be incorporated into the grids to withstand somewhat higher pressures. The pole-unit levers also must be made heavier in order to stand the higher operating speeds and the effects of heavier accelerating springs. This higher contact speed can be obtained by increasing the breaker lift-rod speed and by operating the grid contacts through a multiplying lever system, so that the contact speed exceeds that of the lift rod. A multigrid assembly for three-cycle operation at 287 kv has been built and tested with six units in series per pole. Three of these multiorifice grids are shown in Figure 3, arranged with a side operating rod for actuating all contacts simultaneously.

In the grids shown in Figures 4a and 5, an insulating operating rod along the side of each contact assembly is attached to a rocking arm which operates the contact in the pressure chamber at the top of the grid unit. Clamped between the upper and lower contacts in the closed position is the intermediate contact which separates the pressure and interrupting chambers. The operating rod is raised during the closing operation by the bridging contact crossbar which also carries the lower moving contact. This contact is withdrawn from the grid in the open position to provide electrical isolation. An accelerating spring is compressed during closing, as are the contact springs, and both provide initial acceleration. On opening, the main lift rod is tripped, and

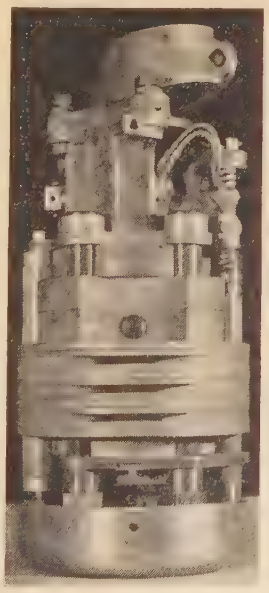
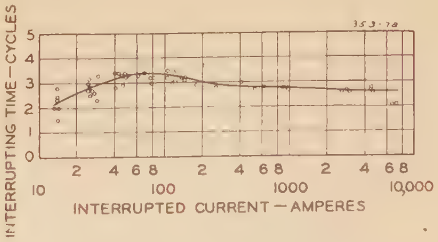
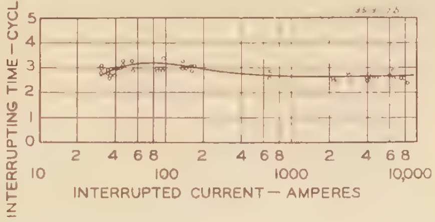


Figure 5. 230-kv five-cycle multiorifice grid-contact assembly with shield removed



(a). 287-kv breaker—3,500,000 kva six grids—198 kv to ground single phase



(b). 230-kv breaker—2,500,000 kva four grids—132 kv to ground single phase

Figure 7. Multiorifice-grid performance in three-cycle high-voltage breakers

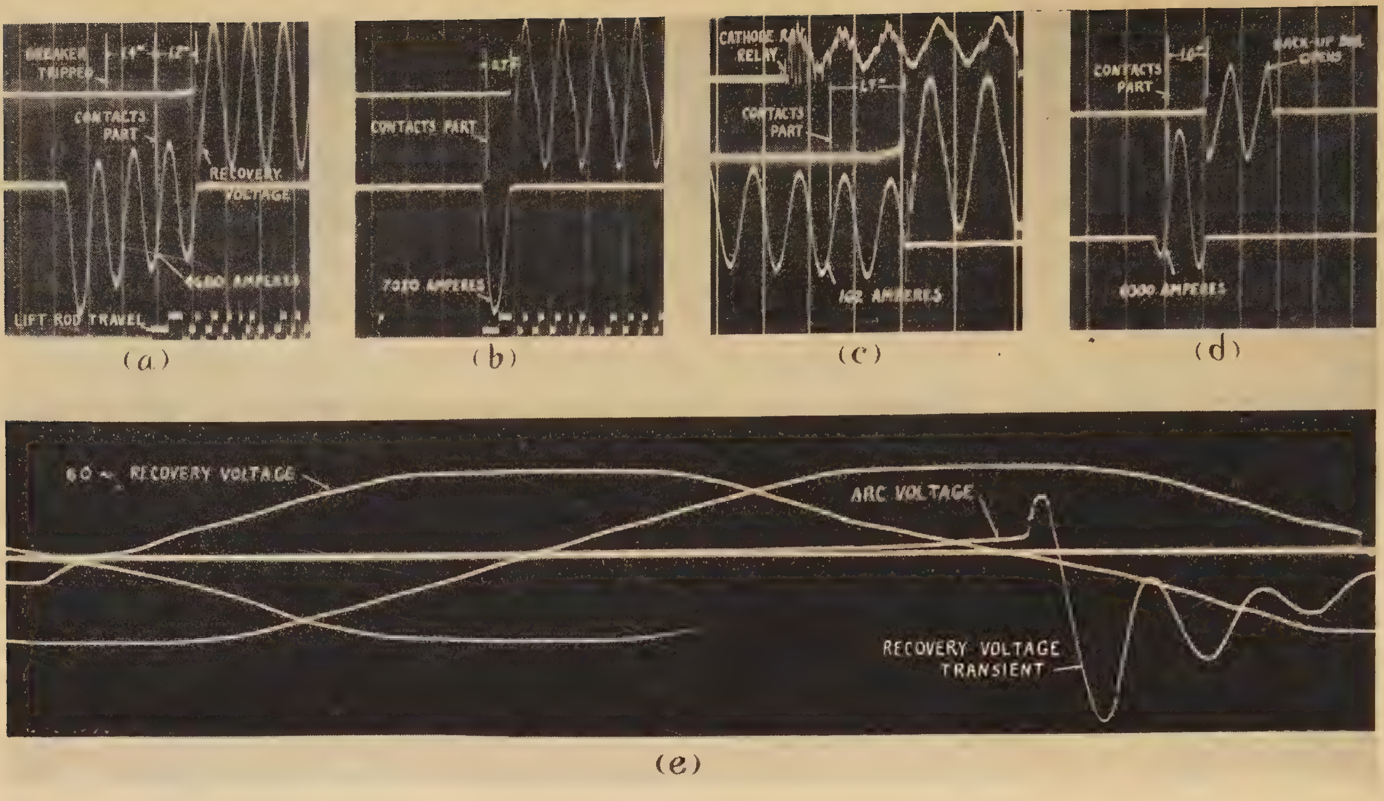


Figure 8. Oscillograms of short-circuit tests on three-cycle multiorifice grid breakers

(a). Six grids, 4,680 amperes, 198 kv to ground

(b). Six grids, 7,020 amperes, 198 kv to ground

(c). Four grids, 162 amperes, 132 kv to ground

(d). Four grids, 8,300 amperes, 132 kv to ground

(e). Cathode-ray oscillogram of same test as 8c

the bridging crossbar drops away from the grids carrying the lower contact. The upper contact opens simultaneously, and the arc is extinguished just before the crossbar leaves the grid to provide an insulating disconnect break in clear oil. Voltage distribution is provided by resistors, while the voltage gradient in the neighborhood of metallic parts is relieved by well-rounded metallic shields.

Table I. 287-Kv Three-Cycle Breaker Data—3,500,000 Kva (7,000 Amperes)

Test No.	Interrupted Current (RMS Amperes)	Arcing Time (Cycles)	Main Gap Per Grid (Inches)	Interrupting Time (Cycles)	Maximum Voltage Across Breaker (Per Cent Normal Crest)
A. Six Grids in Series—198 Kv to Ground—Single Phase—60 Cycles					
1	160	1.4	2.0	2.8	
2	2,640	1.2	1.6	2.6	
3	4,680	1.2	1.6	2.6	
4	4,520	1.3	1.8	2.7	
5	4,600	1.4	2.0	2.8	
6	7,020	0.7	0.7	2.1	
7	7,020	0.7	0.7	2.1	
B. Three Grids (One-Half Pole)—198 Kv to Ground—Single Phase—60 Cycles					
1	160	1.6	2.4	3.0	
2	170	1.4	2.0	2.8	
3	2,440	1.4	2.0	2.8	
4	3,850	1.1	1.4	2.5	
5	3,700	1.2	1.6	2.6	
6	5,900	1.0	1.2	2.4	
C. One Grid (One-Sixth Pole)—33 Kv to Ground—Charging Current—60 Cycles					
1	8	0.2	0.2	1.6	177
2	8	0.1	0.1	1.5	173
3	24	0.1	0.1	1.5	192
4	24	0.3	0.3	1.7	176
5	48	0.9	1.0	2.3	165
6	48	0.8	0.8	2.2	176
7	96	0.8	0.8	2.2	186
8	96	0.8	0.8	2.2	118

Figure 6 shows schematically the results which have been achieved. In 6a the earlier form of 230-kv three-cycle grid arrangement using six grids is illustrated. In 6b by using the multiorifice design, it has been possible to obtain very short arcing time over the entire current range with only four grids. In 6c two grids only are shown for the same purpose, and tests indicate their adequacy for future breaker designs.

Resistors for Voltage Control

A resistor assembly consisting of graphite-impregnated molded blocks mounted in a fiber tube is connected across each multiorifice grid stack, primarily for the purpose of improving the division of voltage among the series interrupters.[8] Figure 15 shows a group of curves indicating the calculated division of voltage between the contact assemblies on the high potential and grounded terminals of a typical high-voltage steel tank breaker as a function of the parallel resistance value for various

Table II. 230-Kv Three-Cycle Breaker Data—2,500,000 Kva (6,300 Amperes)

Test No.	Interrupted Current (RMS Amperes)	Arcing Time (Cycles)	Main Gap Per Grid (Inches)	Interrupting Time (Cycles)
A. Four Grids in Series—132 Kv to Ground—Single Phase—60 Cycles				
1	30	1.6	2.1	3.0
2	162	1.7	2.3	3.1
3	675	1.5	1.9	2.9
4	4,050	1.3	1.5	2.7
5	7,420	1.2	1.3	2.6
6	7,900	1.2	1.3	2.6
7	8,300	1.0	0.9	2.4
B. Four Grids in Series—198 Kv to Ground—Single Phase—60 Cycles				
1	1,150	1.2	1.3	2.6
2	2,650	1.5	1.9	2.9
3	2,400	1.4	1.7	2.8
4	3,350	1.3	1.5	2.7
5	3,350	1.5	1.9	2.9
6	6,200	1.2	1.3	2.6
7	6,560	1.5	1.9	2.9

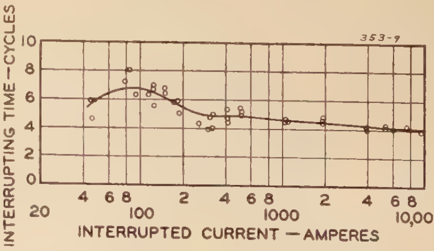
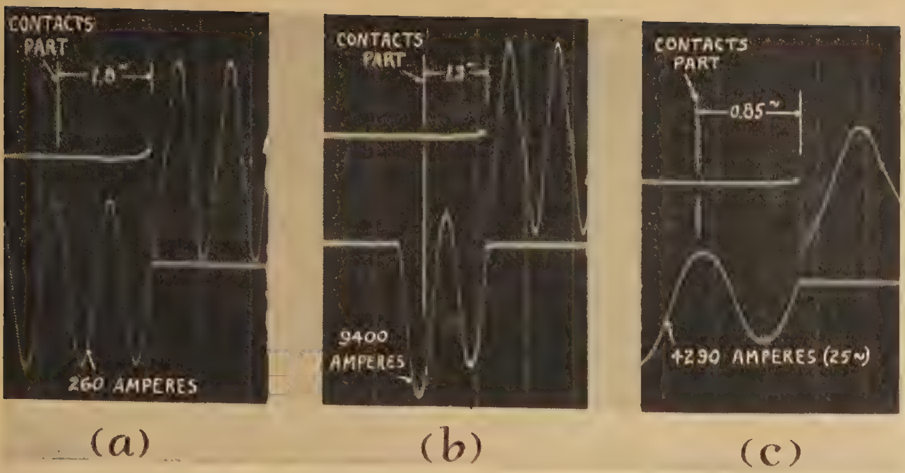


Figure 9. 230-kv five-cycle circuit-breaker performance—two grids—132 kv to ground



(a). Two grids, 260 amperes, 132 kv to ground

(b). Two grids, 9,400 amperes, 132 kv to ground

(c). Two grids, 4,290 amperes, 82.5 kv to ground

Figure 10. Oscillograms of tests on 230-kv five-cycle multiorifice grid breaker

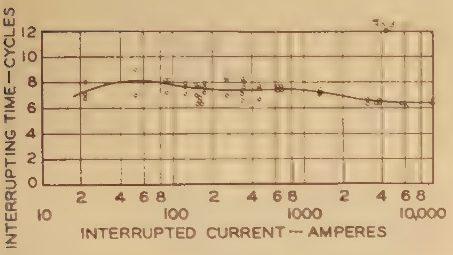


Figure 11. 138-kv eight-cycle circuit-breaker performance—two grids—88 kv to ground

natural frequencies of the transient recovery voltage. The combination of resistance and capacitance involved causes the voltage across the two contact assemblies to be out of phase with each other, and thus the corresponding percentages add up to somewhat more than 100 per cent. The resistance value selected for a 287-kv multiorifice grid design was 360,000 ohms per breaker terminal, giving according to Figure 15 as good or better distribution than with capacitance shielding[7] for all frequencies up to about 2,000 cycles per second. It should be noted that, after the high-frequency transient has been damped out, the normal-frequency voltage will be divided 50 per cent—50 per cent by the resistance. During the interruption of charging currents the voltage distribution is better with resistance dividers than with capacitance shielding, since the voltage builds up across the breaker contacts at normal frequency. Proper voltage distribution during the interruption of charging currents forces each grid to bear its full share of restored voltage. Thus the resistor in combination with sufficient contact speed and oil driving power quickly brings the total dielectric strength of all grids sufficiently high to prevent continued restrikes which might multiply the transient overvoltage. A liberal and quickly achieved contact separation for the oil driving arc has been found to provide good driving pressures even with low currents.

Table III. 230-Kv Five-Cycle Breaker Data—2,500,000 Kva (6,300 Amperes)

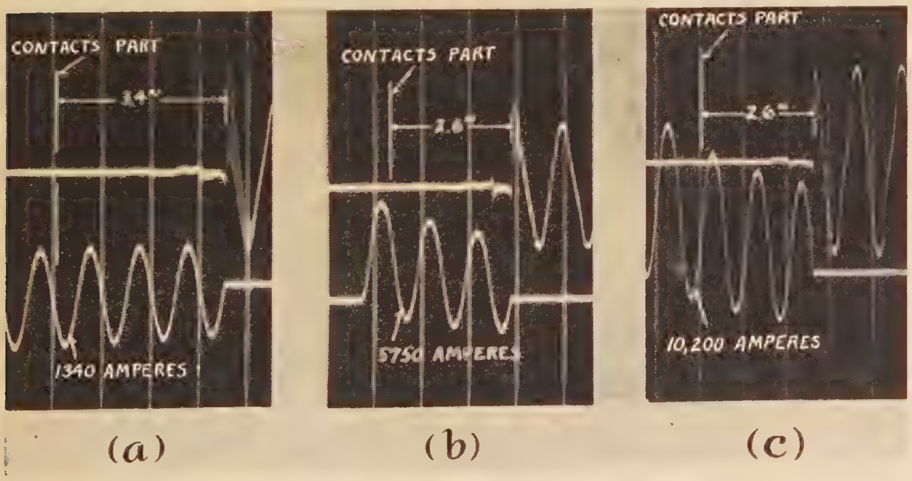
Test No.	Interrupted Current (RMS Amperes)	Arcing Time (Cycles)	Main Gap Per Grid (Inches)	Interrupting Time (Cycles)	
A. Two Grids in Series—132 Kv to Ground—Single Phase—60 Cycles					
1	260	1.8	2.4	4.3	
2	1,060	2.0	2.8	4.5	
3	3,920	1.6	2.0	4.1	
4	5,980	1.5	1.9	4.0	
5	5,340	1.8	2.4	4.3	
6	5,450	1.5	1.9	4.0	
7	7,500	1.7	2.2	4.2	
8	9,400	1.3	1.6	3.8	
B. Two Grids in Series—82.5 Kv to Ground—Single Phase—25 Cycles					
				Time Base	
				25 Cycles	60 Cycles
1	495	1.10	4.1	2.15	5.2
2	485	0.85	3.0	1.90	4.6
3	965	0.90	3.2	1.95	4.7
4	925	1.05	3.9	2.10	5.0
5	3,010	0.80	2.6	1.85	4.4
6	3,080	0.85	2.8	1.90	4.6
7	4,290	0.85	3.0	1.90	4.5
8	4,290	0.85	3.0	1.90	4.6

Lower values of ohmic resistance have been tried experimentally but appear unnecessary with this type of grid. Resistors low in ohmic value are rather bulky to be able to handle the high E^2/R power losses even for time intervals of only a few cycles.

Another advantage obtained with even a moderately high value of resistance in parallel with the interrupter is the reduced voltage gradient along the insulation with the contacts in the open position. The effect of the resistance connection is to keep both ends of the contact assembly at the same potential while the disconnect gap is open. Large well-rounded shields grade this potential off into the oil.

Discussion of Test Data

The curves in Figure 7 show the interrupting time as a function of interrupted current for a three-cycle 287-kv six-unit multiorifice grid breaker tested at 198 kv



(a). Two grids, 1,340 amperes, 88 kv to ground

(b). Two grids, 5,750 amperes, 88 kv to ground

(c). Two grids, 10,200 amperes, 88 kv to ground

Figure 12. Oscillograms of tests on 138-kv eight-cycle multiorifice grid breaker

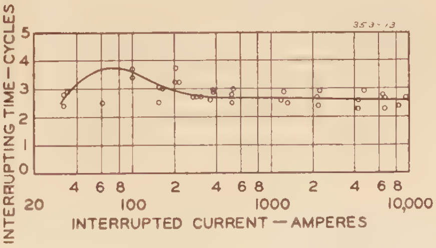
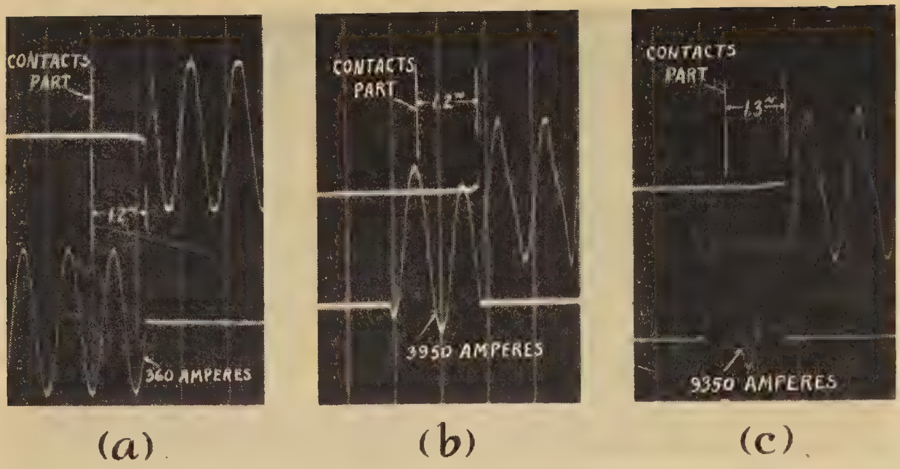


Figure 13. 230-kv three-cycle circuit-breaker performance—two grids—132 kv to ground



(a). Two grids, 360 amperes, 132 kv to ground

(b). Two grids, 3,950 amperes, 198 kv to ground

(c). Two grids, 9,350 amperes, 132 kv to ground

Figure 14. Oscillograms of tests on 230-kv three-cycle multiorifice grid breaker

to ground single-phase, and a three-cycle 230-kv four-unit breaker tested at 132 kv to ground. The first curve, Figure 7a, covers a current range from 14 amperes to 7,000 amperes at a voltage 20 per cent above the normal line-to-ground potential on a 287-kv system. The test at maximum current is a direct demonstration of the ability to meet a 3,500,000 kva interrupting-capacity rating. The interrupting time was less than three cycles for all currents above 210 amperes (six per cent of rating), and did not exceed 3.8 cycles at any current. Table I contains test data for the curve in Figure 7a as well as test data with the full 198 kv across only *one-half* the pole unit of three grids in series. This demonstrates a 100 per cent factor of safety in voltage interrupting ability. Also included in the table are charging-current interrupting tests with one-sixth voltage, or 33 kv, across one of the six multiorifice grids in series with a capacitor bank in the laboratory. A current range from 8 to 96 amperes was available; all interruptions took place in less than three cycles. Part *C* of Table I shows that the maximum voltage across the interrupter during these tests did not exceed 192 per cent where the normal crest voltage is taken as 100 per cent.

Table IV. 138-Kv Eight-Cycle Breaker Data —2,500,000 Kva (10,500 Amperes)

Test No.	Interrupted Current (RMS Amperes)	Arcing Time Both Gaps (Cycles)	Main Gap Per Grid (Inches)	Interrupting Time (Cycles)
Two Grids in Series—88 Kv to Ground—Single Phase—60 Cycles				
1	20	3.3	2.7	7.1
2	52	5.2	6.0	9.0
3	175	4.2	4.4	8.0
4	460	3.7	3.4	7.5
5	1,340	3.4	2.9	7.2
6	3,800	2.7	1.6	6.5
7	3,690	2.6	1.4	6.4
8	3,800	2.8	1.9	6.6
9	5,750	2.6	1.4	6.4
10	6,200	2.3	0.9	6.1
11	10,200	3.0	2.2	6.8
12	10,200	2.6	1.4	6.4

The second curve, Figure 7b, covers a current range from 30 amperes to 8,300 amperes at normal line-to-ground voltage for a 230-kv system, the maximum current corresponding to a three-phase short circuit of 3,300,000 kva. The maximum interrupting time in the low-current range was only 3.4 cycles. By simply increasing the mechanical speed of contact opening, a flat three-cycle characteristic can be obtained over the entire current range. Table II includes not only test data at 132 kv to ground, but also at 50 per cent higher voltage or 198 kv to ground.

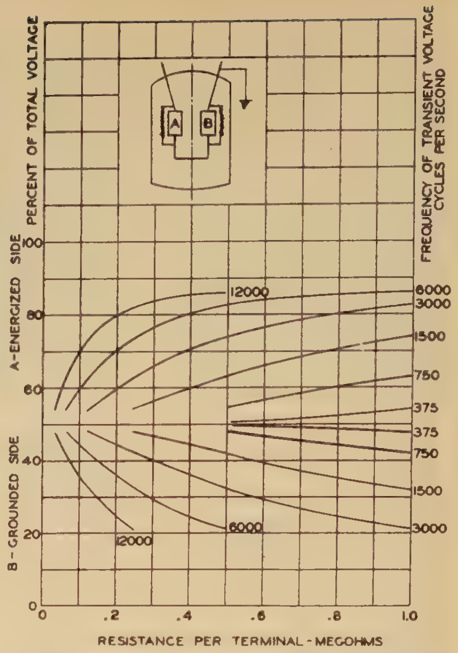
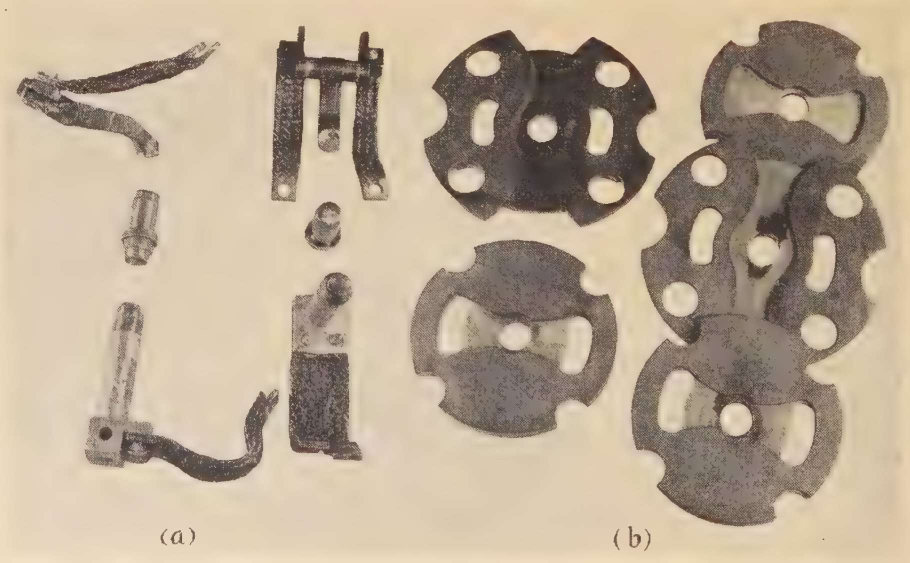


Figure 15. Calculated voltage distribution as a function of parallel resistance for different frequencies of transient recovery voltage

Representative oscillograms for these three-cycle multiorifice grid breakers are shown in Figure 8, including a cathode-ray oscillogram of one of the low-current tests showing that no greater than normal amplitude in the recovery-voltage transient is obtained.

Figure 9 gives a curve of interrupting performance for 230-kv simultaneous-contact-parting multiorifice grids of the type illustrated in Figure 5a for five-cycle rating. The maximum current interrupted was 9,800 amperes at 132 kv, over 50 per cent above the rated interrupted capacity. The remarkable ease



(a). Upper, intermediate, lower contacts

(b). Inlet, orifice, and vent plates

Figure 16. Multiorifice grid parts after interrupting tests

Table V. 230-Kv Three-Cycle Breaker Data—2,500,000 Kva (6,300 Amperes)

Test No.	Interrupted Current (RMS Amperes)	Arcing Time (Cycles)	Main Gap Per Grid (Inches)	Interrupting Time (Cycles)
A. Two Grids in Series—132 Kv to Ground—Single Phase—60 Cycles				
1	32	1.5	3.0	2.9
2	360	1.2	2.1	2.6
3	4,230	1.2	2.1	2.6
4	6,600	1.3	2.4	2.7
5	6,600	0.9	1.3	2.3
6	6,370	1.4	2.7	2.8
7	8,350	1.0	1.5	2.4
8	9,350	1.3	2.4	2.7
B. Two Grids in Series—198 Kv to Ground—Single Phase—60 Cycles				
1	240	1.4	2.7	2.8
2	485	1.7	3.0	3.1
3	963	1.1	1.8	2.5
4	3,950	1.2	2.1	2.6
5	4,510	1.3	2.4	2.7

with which such heavy short circuits are cleared gives evidence of the low arc voltage and the energy of the multiorifice interrupter. This is especially important for 25-cycle application where the arcing time tends to be slightly longer than at 60 cycles, because of the greater time interval before a current zero occurs at which the contacts are parted sufficiently to interrupt. Table III includes not only data at 132 kv 60 cycles, but also at 82.5 kv 25 cycles. Oscillograms of representative low- and high-current tests are shown in Figure 10.

The interrupting performance of the eight-cycle consecutive-contact-parting multiorifice grids for 138 kv, as illustrated in Figure 5b, is shown by Figure 11. The current range covered was from 16 amperes to over 10,000 amperes at 88 kv to ground, or 15 per cent above normal line-to-ground voltage. Data are given in Table IV, and oscillograms for three tests are shown in Figure 12. The simplicity of contact structure makes this design very attractive for application where high-speed operation is not essential.

It has been pointed out earlier in the paper that the ultimate of three-cycle operation at 230 kv with only two units per pole has been demonstrated to be a practical possibility. Using a pair of multiorifice grids similar in operation to the one shown in Figure 5a but equipped with a multiplying linkage for operating the lower moving contacts at high speed, the results shown in Figure 13 were obtained. Currents from 30 to 9,300 amperes at 132 kv to ground single phase were interrupted satisfactorily. All short circuits above 210 amperes were cleared within three cycles. Data are given in Table V not only at 132 kv but at 198 kv to ground single-phase, and Figure 14 includes three representative oscillograms.

All interrupting tests were made in the high-power laboratory described by MacNeill and Batten[9] last year. The circuit voltage-recovery rates during the tests were quite high since the breakers were connected to the transformers with short cable lengths. All tests at 132 kv were on a circuit having a maximum circuit voltage-recovery rate of 3,500 volts per microsecond. At 198 kv the corresponding rate was 5,500 volts per microsecond.

An examination of all oscillograms shows not only short arcing time but very low arc voltage. The energy dissipated in the new interrupter where two grids are used for five-cycle operation is approximately one third to one half of that commonly considered acceptable in present-day interrupters. For three-cycle operation at 230 kv, the reduction in the number of interrupters lowers the total energy dissipation to approximately one fourth that of current designs.

There is a complete absence of demonstration during interruption. Measurements of tank pressure with two multiorifice grids show maximum pressure impulses of only 60 pounds when interrupting high currents of approximately 10,000 amperes.

Multiorifice grids have been tested for large numbers of consecutive high-current interrupting tests. Examination of the parts after such tests shows very little burning and erosion of the orifice plates or other grid parts. For example, Figures 16a and 16b show several orifice plates and contacts taken from a grid which had withstood 22 interrupting tests from 200 to 10,000 amperes. All but eight of these tests were above 3,000 amperes.

Conclusions

A multiorifice type of interrupter has been found to have excellent interrupting ability in the high-power laboratory. Its arc-energy dissipation is markedly lower than with present interrupters. In the 230-kv three-cycle field the greatest improvements are possible, because the number of interrupters per pole unit can be reduced from six to two.

The interruption of low currents is achieved with short arcing time by this interrupter, and means have been introduced into its design to restrict overvoltages due to restriking.

A saving in critical materials is made possible by the use of this interrupter which is consistent with the current war effort.

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Substation-Transformer Emergency Overloading Practice

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MEMBER AIEE

Synopsis: Although much has been written in recent years regarding allowable overloading of transformers, the practical matter of matching existing load curves with such allowable limits has continued to present some difficulties. The interim report of the AIEE transformer subcommittee gives one approach to the problem, taking into account the ambient temperature and the duration of the overload. However, in order to take full advantage of the transformer's inherent load-carrying ability, it is necessary to consider additional factors such as the individual transformer characteristics and the specific shape of the overload curve.

This paper shows a method by which all of the influencing factors can be taken into account easily. Allowable overloads thus determined are often considerably higher than those indicated by the AIEE method. The application of the proposed method is sufficiently simple to justify a semiannual analysis of expected overloads with respect to substation-transformer emergency ratings. It consists in

1. Determining the allowable 24-hour steady overload that can be carried by the transformer without exceeding certain agreed-upon limits.
2. Converting the specific load curve to a 24-hour steady load which would cause equal aging of the transformer insulation.

In this manner both the transformer load-carrying ability and the actual irregular load are reduced to a common basis for ready comparison. A 24-hour overload period is ordinarily of ample duration to allow for replacement or repair of faulty equipment.

Nomographs are included for quick determination of allowable transformer 24-hour overloads, taking into account ambient temperature, top-oil rise, average copper rise, and ratio of copper to core loss. Empirical multipliers are also given which facilitate the conversion of the load curve to equivalent 24-hour steady load in no more time than it would take to calculate load factor.

ALLOWABLE overloading of transformers during emergency conditions has received considerable attention during recent years, and many valuable papers on the subject have appeared in the technical literature. The object of this paper is to describe the method developed by The Detroit Edison Company for determining the allowable emergency overloading of substation transformers.

In any consideration of allowable transformer overloading, there are three factors which must be taken into account: namely, the magnitude of the overload, its duration, and its frequency of occurrence. The magnitude of the overload is dependent upon the number of transformer units in a substation, the duration is dependent upon the time required for replacement or repair of faulty equipment, and the frequency of occurrence must be obtained from experience records.

Magnitude of Overload

Many substations consist of two or more transformers, or transformer banks, of equal size arranged in such a way that each may carry a normal day-to-day load of a value somewhere near its name-plate rating. When a transformer fails, or other trouble develops causing that unit to be shut down, it is obviously desirable that the remaining units of the substation have sufficient overload capacity to carry the entire load until the defective unit can be replaced or other trouble repaired. This means in the case of a substation having only two transformer units, it would be highly desirable that each unit have an emergency rating equal to about twice its allowable day-to-day loading. Where there are three or more transformer units in the substation, the emergency rating of each can be correspondingly lower.

There are also many suburban-type substations each consisting of a single bank of three single-phase transformers, connected in delta delta. In case of failure of one of these transformers, it is customary to cut it out of service, leaving the remaining two to carry the load in open delta until the defective transformer can be replaced. In these cases the emergency rating of the transformers should be about 70 per cent greater than their allowable day-to-day loading.

Duration of Overload

It is the practice of the company to keep a limited number of emergency spare transformers of appropriate sizes at strategic locations for use in the event of failure of operating units. Experience has shown that a defective transformer, other than a very large unit such as installed at bulk power step-down stations, can usually be replaced with a spare within about 24 hours.

Frequency of Occurrence

Substation records indicate that transformer failures can be expected about once every 16 years per substation. If the transformer is radially fed by underground cable, the expectancy of cable failure must also be taken into account; likewise, any other failures that may result in the loss of a transformer from the system must be considered. Consequently, it has been assumed that, all things considered, any one transformer unit may be subjected to emergency operation not oftener than about once every five or ten years.

Need for Simple Method

In much of the technical literature on the subject of transformer overloading it has been the practice to express allowable emergency overloads in terms of steady loads of given durations following either a period of no load or a period of full load. The recent interim report of the AIEE transformer subcommittee, dated June 22, 1942, follows this same plan. While this defines the load-carrying ability of the transformer in quite specific terms, the expression is not wholly compatible with the irregular shape which characterizes the majority of actual load curves.

By means of the so-called rigorous method, based upon the generally accepted theory governing transient changes in oil and copper temperature with changing transformer load, it is possible to determine whether it is feasible to carry an overload of any given shape and magnitude. While this method is useful in checking isolated cases, it is far too cumbersome and time consuming for routine application in connection with hundreds of transformer installations.

The method described in this paper is comparatively simple and its application requires little time. It consists in first expressing the allowable transformer overload in terms of a steady 24-hour load, and then converting the potential

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overload curve to a steady 24-hour load of such magnitude that it will cause the same insulation aging as the irregular curve. Thus the transformer capabilities and the potential overload are reduced to a common basis for ready comparison.

Allowable Emergency Transformer Overloading

The well-known "eight-degree rule," which states that the rate of insulation aging doubles for each eight degrees centigrade increase in temperature, has been generally accepted as a rough measure in determining the life of transformer insulation under varying temperature conditions. There are fairly wide variations from the strict application of the rule, as shown by tests of F. M. Clark.[1] There is also no definite generally accepted bench mark from which to start. L. C. Nichols[2] shows $1^1/_3$ years' life for 105 degrees centigrade temperature compared with V. M. Montsinger's[3] seven years' life for the same temperature.

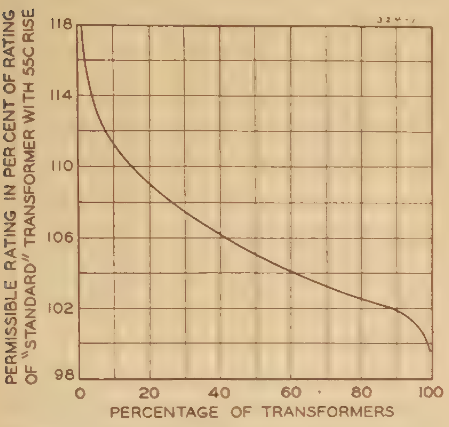

Figure 1. Variation in load-carrying ability of 126 substation transformers ranging in size from 100 to 10,000 kva

In the practice adopted by The Detroit Edison Company, the allowable emergency overload capacity of transformers has been based upon the "eight-degree rule" and Nichols'[2] life curve including safety factor. Montsinger's[3] curve could have been used just as well. No brief is held for either curve; both are based on the "eight-degree rule," and neither authority claims to foretell with any degree of accuracy the actual transformer life. By following either curve and using a reasonable hot-spot temperature as a guide, all transformers can be treated alike and subjected to a uniform degree of insulation aging during emergencies.

On the basis that the emergencies occur only once in five or ten years and last for a single 24-hour load period, it was thought reasonable to allow one per cent life consumption for each 24-hour emergency. This was found on Nichols' curve to correspond to a hot-spot temperature of 120 degrees centigrade for 24 hours, which was considered to be a reasonable average value provided that:

1. The maximum hot-spot temperature never exceeds 150 degrees centigrade.
2. The maximum top-oil temperature never exceeds 115 degrees centigrade.

Investigations showed that with the worst conditions expected to be encountered these upper limits would not be exceeded. As an over-all rule it was decided that under no conditions should the peak load on a transformer be allowed to exceed 200 per cent of name-plate rating.

VARIATIONS IN TRANSFORMERS

Experience indicates that there is considerable variation in temperature rise and corresponding load-carrying ability between different transformers of the same name-plate rating. One can well afford to take advantage of this extra marginal capacity. Figure 1 shows the variation in load-carrying ability of 126 substation transformers ranging in size from 100 to 10,000 kva. The 126 transformers are compared with a "standard" 55 degree centigrade rise transformer having a 45 degree centigrade top-oil rise and a ratio of full-load copper loss to iron loss of two. It is seen that about 50 per cent of the transformers studied permit five per cent greater continuous loads than the 55 degrees centigrade transformer and, that five per cent of the transformers permit 13 per cent greater continuous loads without exceeding the allowable temperature limits.

The transformer variables which determine the amount of overload that can be carried without exceeding a given hot-spot temperature rise are top-oil rise, average copper rise, and ratio of copper loss to core loss. The ambient temperature then fixes the actual hot-spot temperature and resultant insulation aging.

AMBIENT TEMPERATURES

United States Weather Bureau statistics for the Detroit area show that summer (May through October) ambient temperatures seldom exceed 35 degrees centigrade. Similarly, for winter (December through February) there are a few days with ambient temperatures as high as 13 degrees centigrade. On the basis of these records the summer and winter ambient temperatures selected for establishing allowable emergency transformer loading were 35 degrees centigrade and 13 degrees centigrade, respectively.

NOMOGRAPHS

As a means for determining quickly the allowable 24-hour emergency loading of transformers for the chosen ambient temperatures, the nomographs shown in Figures 2 and 3 were developed. They are based on the following generally accepted fundamental relationship:

$$T_{hs} = T_f\left(\frac{1+NR^2}{1+N}\right)^{0.8} + T_{cu}R^2 + T_a \qquad (1)$$

where

T_{hs} = hot-spot temperature, degrees centigrade
T_f = top-oil rise at full load, degrees centigrade
N = ratio of copper loss to core loss at full load
R = ratio of load to name-plate rating
T_{cu} = copper rise above top oil at full load plus ten degrees centigrade allowance for hot-spot differential, degrees centigrade
T_a = ambient temperature, degrees centigrade

To demonstrate the use of the nomographs, two examples are shown in dashed lines for self-cooled transformers, A and B, having the characteristics shown in Table I.

Figure 2. Nomograph for determining allowable summer emergency overload capacity of self-cooled transformers

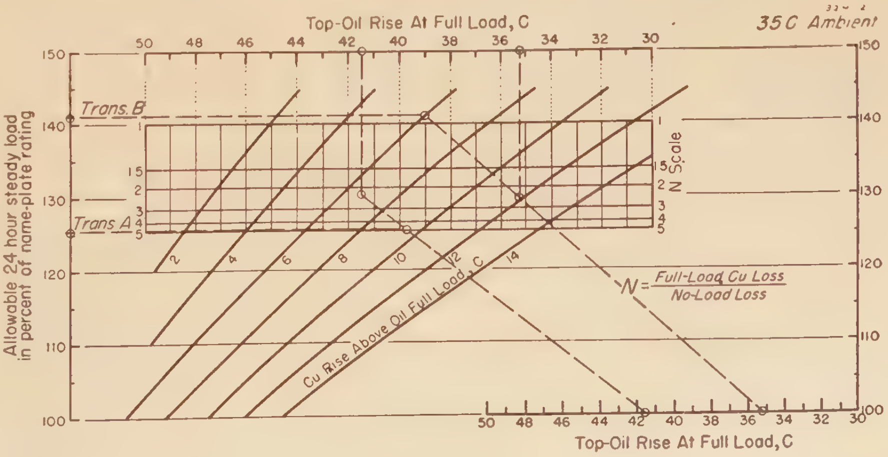

Table I

	Transformer *A*	Transformer *B*
Name-plate rating	500 kva	1,500 kva
Copper loss	4,070 watts	10,850 watts
Core loss	1,750 watts	4,296 watts
N	2.32	2.53
Top-oil rise	41.5 degrees centigrade	35.2 degrees centigrade
Average copper rise above top oil	9.5 degrees centigrade	6.0 degrees centigrade
Weight of core and coils	3,200 lb	9,700 lb
Weight of tank	1,600 lb	4,950 lb
Weight of oil	2,200 lb	6,050 lb

The procedure consists of first locating the point of intersection of a vertical line from the upper scale of top-oil rise with a horizontal line of the proper *N* value. Then line up a straight edge between this point and the top-oil rise on the bottom scale. With the straight edge held in this position, its intersection with the curve for the proper copper rise above oil determines the allowable 24-hour steady load. This value, expressed as per cent of name-plate rating, is read by projecting the point of intersection horizontally to the scale on the left.

From the nomographs for summer and winter conditions, the allowable 24-hour steady overloads for the two examples are:

	Transformer *A* (Per Cent)	Transformer *B* (Per Cent)
Summer	126	141
Winter	146	162

Load-Curve Conversion

As stated before, it is difficult to compare an irregular-shaped overload curve with values of allowable transformer overloads. However, by converting the actual overload curve to an equivalent steady aging load, it can be compared directly with the allowable 24-hour steady load previously determined for the transformer. To do this, the 24-hour day is divided into three parts: morning, afternoon, and evening. An average load is obtained for each of these periods in the following manner. The average of the three highest consecutive hourly readings any time between midnight and noon is designated as the average morning load. The average of the three highest consecutive hourly readings any time after noon and up till 9 p.m. is designated as the average afternoon load. The average of the three highest consecutive hourly readings any time between the selected afternoon readings and midnight is designated as the average evening load. There is no fixed time for dividing afternoon from evening. The three groups of consecutive hours should be so selected that the resultant equivalent aging load obtained by substituting the three average loads in the following formula is a maximum:

$$EAL = 0.10L_{\min} + 0.30L_{\text{int}} + 0.52L_{\max} \qquad (2)$$

where

EAL = equivalent steady aging load

$L_{\min}$ = the smallest of the three average loads

L_{int} = the intermediate of the three average loads

$L_{\max}$ = the largest of the three average loads

Three general examples are shown: Figure 4 for typical industrial winter load, Figure 5 for typical combined industrial and residential summer load, and Figure 6 for typical residential summer load. In each of these figures, curve 2 is the equivalent steady aging load corresponding to the potential irregular-shaped overload, curve 1.

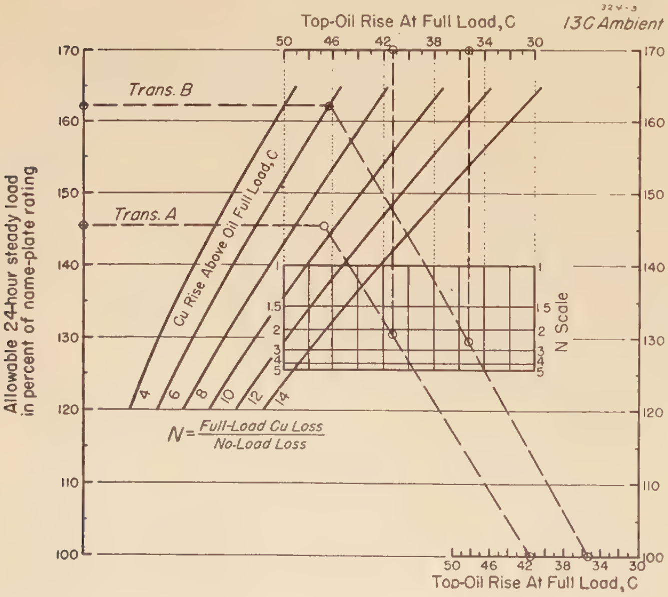

Figure 3. Nomograph for determining allowable winter emergency overload capacity of self-cooled transformers

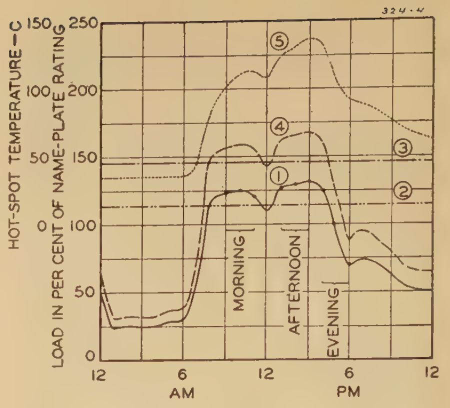

Figure 4. Typical industrial winter load and allowable emergency overload of transformer *A*

1. Potential winter emergency transformer overload based on existing substation load, per cent
2. Equivalent steady aging load of curve 1, per cent
3. Allowable 24-hour steady winter overload of transformer *A*, per cent
4. Maximum allowable winter overload, per cent
5. Hot-spot temperature corresponding to overload curve 4, degrees centigrade

Considering the industrial winter load, curve 1 of Figure 4, the 9, 10, and 11 o'clock readings are averaged to give an average morning load of 123 per cent of name-plate rating of the transformer carrying the load during the emergency. Similarly, the 1, 2, and 3 o'clock readings give an average afternoon load of 129 per cent and the 4, 5, and 6 o'clock readings give an average evening load of 98 per cent of name-plate rating.

Then, according to formula 2.

$EAL = 0.10 \times 98$ per cent $+ 0.30 \times 123$ per cent $+ 0.52 \times 129$ per cent $= 114$ per cent of name-plate rating as shown by curve 2 in Figure 4. Thus, this particular load which has a peak of 131 per cent will cause the same amount of transformer aging as a steady 24-hour load of 114 per cent.

Assuming that this overload is to be carried by transformer *A* previously referred to, its allowable emergency overload capacity, 146 per cent, has been shown in Figure 4 as curve 3. A comparison of these two values, 114 per cent and 146 per cent, indicates that the transformer has ample overload capacity under these conditions.

The maximum load of the shape shown by curve 1 in Figure 4 that can be carried by the transformer is obtained readily by direct proportions and is shown by curve 4. In other words, the maximum peak load that can be carried by the transformer under these conditions is the peak of curve 1, 131 per cent, multiplied by the ratio, 146/114 which gives 168 per cent load in terms of name-plate rating. So much for transformer *A*.

Next, it is assumed in Figures 5 and 6 that the loads shown are to be carried by transformer *B*, and, correspondingly, curves 4 and 3 show the transformer capabilities to be compared respectively with the potential load curve 1 and its equivalent, curve 2.

Accuracy of Method

The load-curve conversion method described above, which may be referred to as the short-cut method, is intended only for use with load factors in the usually encountered range of 50 to 90 per cent. The great majority of load curves fall in this range. When a load has 90 per cent or better load factor, it is probably safest to assume it to be a continuous round-the-clock load. Such loads are usually typical of electrochemical plants, oil refineries, ice plants, and other similar industries not too frequently encountered.

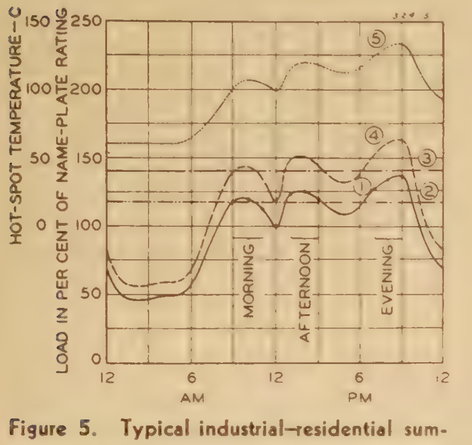

Figure 5. Typical industrial–residential summer load and allowable emergency overload of transformer B

1. Potential summer emergency transformer overload based on existing substation load, per cent
2. Equivalent steady aging load of curve 1, per cent
3. Allowable 24-hour steady summer overload of transformer *B*, per cent
4. Maximum allowable summer overload, per cent
5. Hot-spot temperature corresponding to overload curve 4, degrees centigrade

The accuracy of this short-cut method has been checked by comparison with the rigorous method in which it has been assumed that the hot-spot temperature rise of copper over top oil varies as the square of the load, that the hot-spot differential is ten degrees centigrade at full load, and that the top-oil rise under transient load conditions varies in accordance with the following generally accepted formula:

$$\theta=\theta_u\left(1-e^{-\frac{Wt}{C\theta_u}}\right) \qquad (3)$$

where

θ = top-oil rise at end of time t
W = total losses
C = thermal capacity of core and coils, tank and oil
θ_u = ultimate oil rise (varying as 0.8 power of losses for self-cooled transformers)
e = 2.718
t = time

The three examples given in Figures 4, 5, and 6 show hot-spot temperatures, curve 5, calculated in the above manner for the 24-hour allowable emergency loads, curve 4, determined by the short-cut method. The hot-spot temperatures converted to insulation aging and per cent life consumption indicate errors of −1.6 per cent, −2.0 per cent, and + 2.0 per cent in allowable peak loads for the three examples cited. In most cases it will be found that the short-cut method gives results accurate to within plus or minus five per cent in allowable loads.

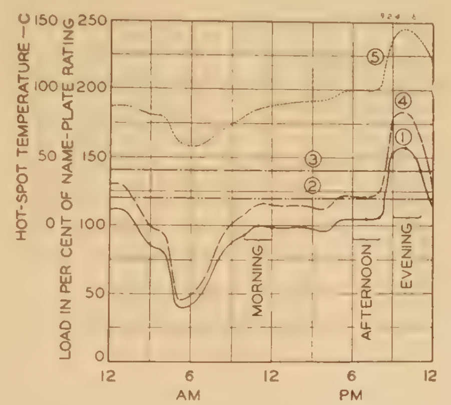

Figure 6. Typical residential summer load and allowable emergency overload of transformer B

1. Potential summer emergency transformer overload based on existing substation load, per cent
2. Equivalent steady aging load of curve 1, per cent
3. Allowable 24-hour steady summer overload of transformer *B*, per cent
4. Maximum allowable summer overload, per cent
5. Hot-spot temperature corresponding to overload curve 4, degrees centigrade

Load-Factor Method

It has been suggested at times that load factor be used in determining the ability of a transformer to carry a given overload. In general, the lower the load factor, the higher the allowable peak load; however, there are such wide variations in allowable peak loads for curves of equal load factor but different shape, that load factor alone cannot be used as a reliable indicator. For instance, Figure 7 presents plotted data showing the range of allowable peak load values for 240 different load curves of various shapes and load factors, each of which has equal aging effect on the transformer insulation. It is seen, for example, that for load curves having approximately 60 per cent load factor, the allowable overloads range from 11 per cent to 43 per cent of the equivalent steady load. Montsinger[4] has presented similar conclusions and stated that two load curves, each with 50 per cent load factor, may vary in allowable overload from 16 per cent to 40 per cent on the basis of equal aging.

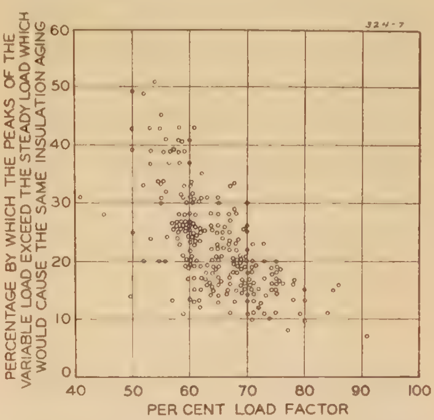

Figure 7. Range of peak load values for 240 different load curves of various shapes, each of which has equal aging effect on the transformer insulation

Routine Application of Method

It has been company practice for a good many years to obtain transformer load readings from recording ammeters at unattended substations and from operators' log sheets of hourly load readings at attended substations. It was the custom to obtain summer and winter peak loads from these records and, with some rough regard to the number of hours of peak load duration, to establish firm ratings for each substation.

In 1939 the short-cut load-conversion method of analyzing load curves was developed and put into practice. This permitted substation firm ratings to be increased materially with safety, because it insured uniform treatment of all transformers, irrespective of the shape of the load curve. Without this assurance of equal treatment and close control, large factors of safety were necessary to insure safe operation of the poorest transformer in combination with the most severe load curve of any general class. The application of this new method requires some additional effort when the load curves are analyzed, but this effort has proved to be more than justified in view of the increased substation firm ratings thus established.

Figure 8 is typical of the continuing records kept for all substations. Dexter substation is of the suburban type and consists of three 333-kva transformers connected in closed delta. Under emergency conditions there would be two transformers operating in open delta with a combined name-plate capacity of 577 kva. Since 1939, the allowable summer peak load has ranged between 151 and 158 per cent of emergency name-plate capacity and the allowable winter peak load between 187 and 197 per cent, indicating some slight change in the shape of the load curve from year to year. In this particular case, the summer load is the limiting condition as evidenced by the per cent usage which has increased from 69 per cent in 1939 to 105 per cent in 1942, as compared with an increase of winter usage from 58 to 86 per cent. If the load continues to grow in that community, the 105 per cent usage for the summer of 1942 indicates that a transformer change is probably needed before the summer of 1943.

In such cases the short-cut load-conversion method is verified by the more accurate rigorous method to be sure that the change is needed in fact. Comparative calculations of this type, including those made during the development of the short-cut method, show an average error of 0.6 per cent for some 32 cases. In one case the error was 9 per cent on the low side, in two cases 5.5 per cent on the high side, and in all other cases it was well within the expected plus or minus 5 per cent.

While not within the scope of this paper, mention should be made that all substation equipment—not just transformers alone—must be considered when studying allowable overloads. Circuit breakers, busses, disconnecting switches, and all such equipment must receive due consideration.

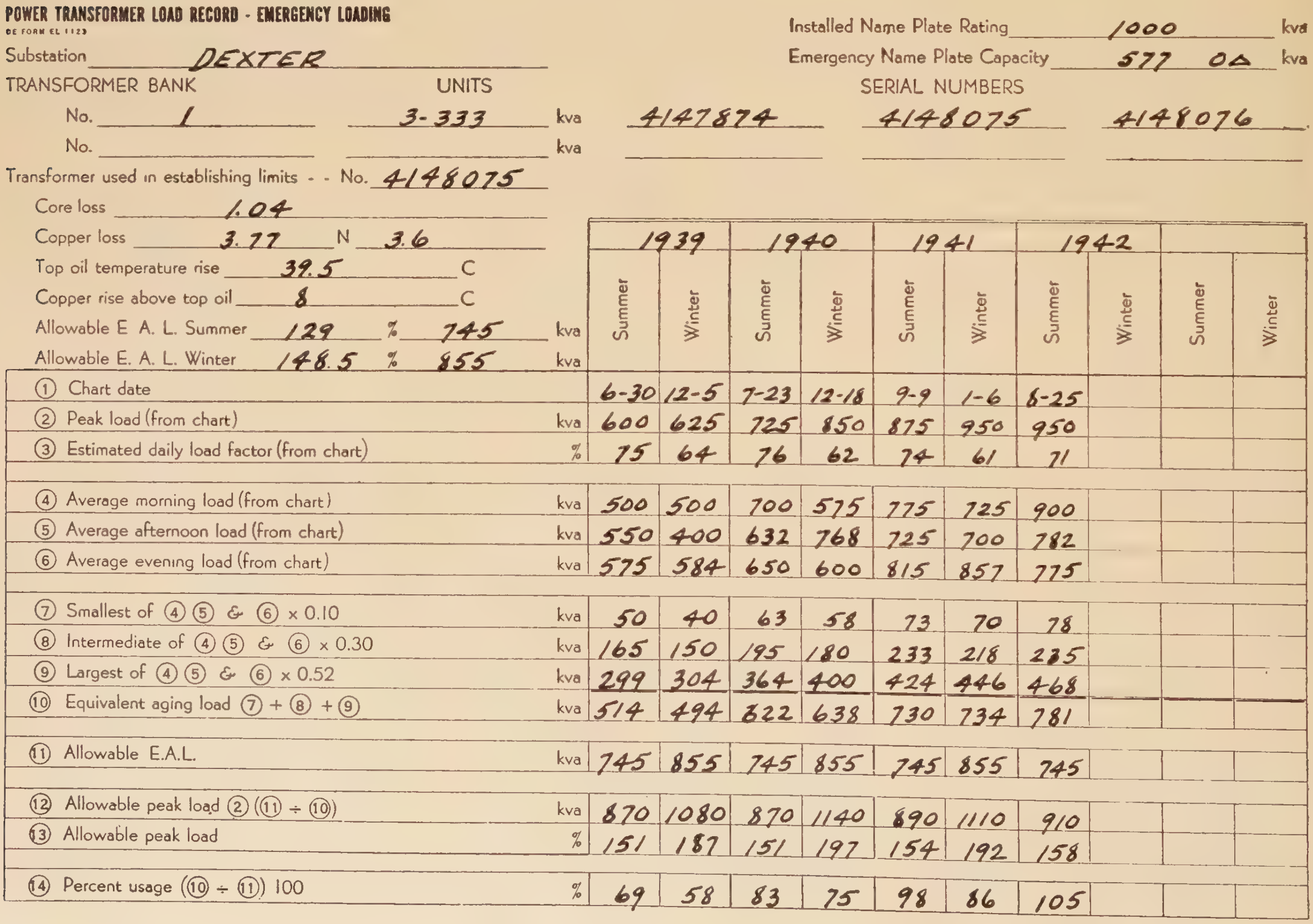

Figure 8. Typical record of substation loading

POWER TRANSFORMER LOAD RECORD - EMERGENCY LOADING
GE FORM EL 1123

Installed Name Plate Rating 1000 kva
Emergency Name Plate Capacity 577 OΔ kva

Substation DEXTER

TRANSFORMER BANK UNITS SERIAL NUMBERS
No. 1 3-333 kva 4147874 4148075 4148076
No. ____ ____ kva

Transformer used in establishing limits - - No. 4148075
Core loss 1.04
Copper loss 3.77 N 3.6
Top oil temperature rise 39.5 C
Copper rise above top oil 8 C
Allowable E A. L. Summer 129 % 745 kva
Allowable E. A. L. Winter 148.5 % 855 kva

		1939		1940		1941		1942			
		Summer	Winter	Summer	Winter	Summer	Winter	Summer	Winter	Summer	Winter
① Chart date		6-30	12-5	7-23	12-18	9-9	1-6	6-25			
② Peak load (from chart)	kva	600	625	725	850	875	950	950			
③ Estimated daily load factor (from chart)	%	75	64	76	62	74	61	71			
④ Average morning load (from chart)	kva	500	500	700	575	775	725	900			
⑤ Average afternoon load (from chart)	kva	550	400	632	768	725	700	782			
⑥ Average evening load (from chart)	kva	575	584	650	600	815	857	775			
⑦ Smallest of ④ ⑤ & ⑥ × 0.10	kva	50	40	63	58	73	70	78			
⑧ Intermediate of ④ ⑤ & ⑥ × 0.30	kva	165	150	195	180	233	218	235			
⑨ Largest of ④ ⑤ & ⑥ × 0.52	kva	299	304	364	400	424	446	468			
⑩ Equivalent aging load ⑦ + ⑧ + ⑨	kva	514	494	622	638	730	734	781			
⑪ Allowable E.A.L.	kva	745	855	745	855	745	855	745			
⑫ Allowable peak load ② (⑪ ÷ ⑩)	kva	870	1080	870	1140	890	1110	910			
⑬ Allowable peak load	%	151	187	151	197	154	192	158			
⑭ Percent usage (⑩ ÷ ⑪) 100	%	69	58	83	75	98	86	105			

Auxiliary Cooling to Increase Emergency Rating

As stated before, the need for the highest emergency rating is in those substations having only two transformer units. Even by working the transformers to the 24-hour, 120 degrees centigrade hot-spot limit, the emergency rating may still be inadequate as compared with the normal day-to-day loading limit. In such cases, auxiliary cooling is sometimes employed to increase the emergency rating so that it is no longer the bottleneck in substation loading.

Either fans or water spray can be used. Where the auxiliary cooling is desired for emergency use only and a reliable supply of water is available, the water-spray method is by far the more economical, and at the same time it gives a much larger increase in rating. A water-spray ring for a 6,000- or 10,000-kva three-phase transformer can be installed for a few hundred dollars, and it increases the emergency rating some 30 or 40 per cent. Figure 9 presents the results of a load test on a 6,000-kva transformer showing a 37 per cent increase in emergency rating for loads at 120 degrees centigrade hot-spot temperature.

Permanently installed fans on large transformers usually represent an investment of thousands instead of hundreds of dollars, and, in the 120 degrees centigrade hot-spot range, they increase the emergency rating a matter of only 10 to 15 per cent. Portable fans represent a much lower investment and have the further advantage that one set of fans can serve any one of many locations at the time of emergency; however, they too increase the emergency rating only moderately. Figure 10 shows comparative test data for a bank of three 5,000-kva self- and fan-cooled transformers, with and without the fans in operation. Here it is seen that, in the emergency overload range of operation, the benefit from fans is relatively moderate.

Fans do, however, have a definite field of application. They are most valuable where the day-to-day rating of a transformer must be increased, either to forestall the installation of added capacity, or to help carry the normal load through the hot summer months. Water spray is not recommended for day-to-day use and should be considered only where the emergency rating needs a boost.

Tests at Bulk Power Step-Down Stations

The company has seven bulk power stations, where voltage is stepped down from 120 to 24 kv, each consisting of two or more transformer banks of 15,000- to 45,000-kva rating. Load readings from these stations are analyzed semiannually along with the load readings from distribution substations. However, in the case of bulk power stations when the load has reached a value such that the potential emergency overload approaches the calculated limit of the transformers, actual field tests are conducted, subjecting each transformer bank and all its related equipment to one emergency overload cycle, the equivalent of 24 hours operation at 120 degrees centigrade hot-spot temperature. During the past two years, three banks of three 10,000-kva self-cooled transformers, and two banks of three 5,000-kva self- and fan-cooled transformers have been tested. Figures 10 and 11 show typical results of such tests. Table II shows the comparison between calculated and test values for final top oil and final average copper temperatures.

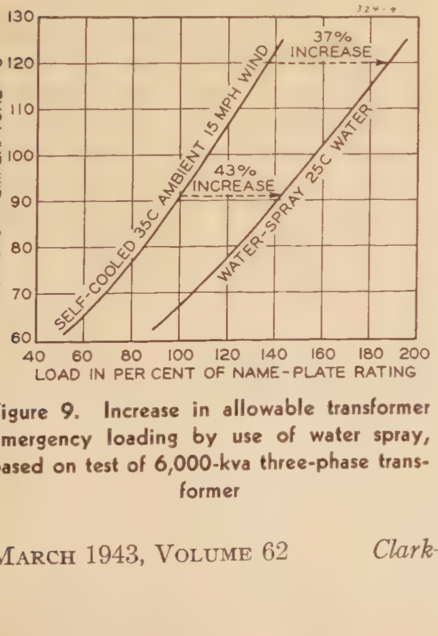

Figure 9. Increase in allowable transformer emergency loading by use of water spray, based on test of 6,000-kva three-phase transformer

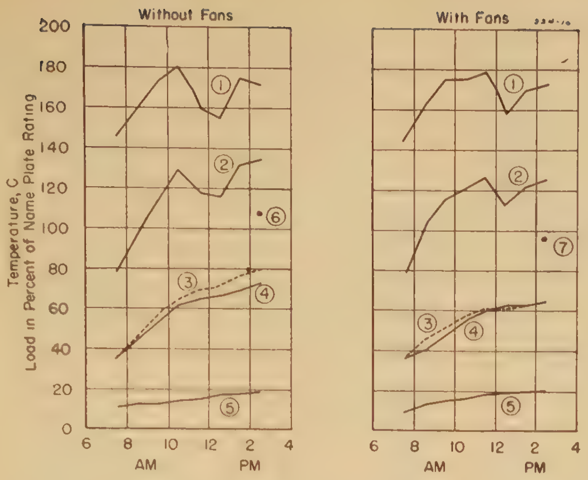

Figure 10. Emergency overload test of a bank of three 5,000-kva self- and fan-cooled transformers

1. Load in per cent of name-plate rating
2. Hot-spot temperature, degrees centigrade (calculated)
3. Top-oil temperature, degrees centigrade (calculated)
4. Top-oil temperature, degrees centigrade (test)
5. Ambient temperature, degrees centigrade
6. Average copper temperature at shutdown (without fans); 111 degrees centigrade by calculation, 107 degrees centigrade by test
7. Average copper temperature at shutdown (with fans); 96 degrees centigrade by calculation, 98 degrees centigrade by test

These of necessity were field tests, the results of which are influenced by such uncontrolled factors as the sun and clouds, ambient temperature, and wind. All tests were started on calm, sunny days but conditions sometimes changed before the tests could be completed. Under the circumstances, the test and calculated values were considered to be in reasonable agreement.

The importance of conducting such emergency tests cannot be overemphasized, even if they do nothing more than acquaint the operators with what to expect when the station equipment is overloaded. Tests are bound to bring to light unusual conditions that might otherwise be overlooked. It is one thing to set up emergency-load limits of from 150 to 200 per cent of name-plate rating for a large group of stations, and another to be sure that everything in a particular station including the bus, circuit breakers, cable, current transformers, disconnect switches, and so forth, can carry the specified overload safely. The anticipated emergency usually is thought of as likely to happen at some indefinite time in the distant future, the result being that certain lesser details are currently overlooked. On the other hand, if an overload test is scheduled definitely, the responsibility for considering every possible contingency becomes immediate.

Such tests insure that the established overload limits can be met safely under the more trying conditions of an actual emergency.

Potential Difficulties to Be Avoided

Experience thus far has indicated the possibility of several types of trouble during emergency-overload periods, none of which is associated with the limits in thermal ability of the equipment to carry the load safely. Any one of these might prove serious if first encountered during an actual emergency, rather than during a staged test.

Among such possible troubles are those related to relaying. It is entirely possible that although relay settings may be such as to give proper breaker operation for any system fault under normal loading conditions, they may cause faulty operation when the transformers are carrying 150 to 200 per cent of name-plate rating. All possible load and fault combinations therefore should be carefully checked.

Transformer oil will expand beyond normal limits when the unit is carrying high overloads and may cause trouble, particularly in the case of transformers served by a common inert gas system where the oil is likely to back up into the gas equipment. In some cases it has been necessary to set up instructions for emergency lowering of the oil level in order to avoid such occurrence.

Oil-filled gas-pressure bushings may leak because of increased pressure at the high temperature if proper adjustment of oil level and gas pressure is not made.

Switchboard ammeters often will be found to go off scale during an emergency. If there is any time an operator needs to

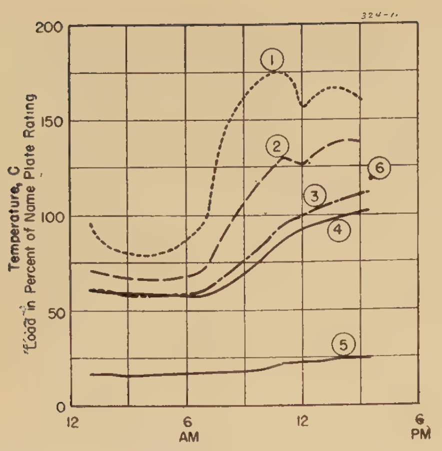

Figure 11. Emergency overload test of a bank of three 10,000-kva self-cooled transformers

1. Load in per cent of name-plate rating
2. Hot-spot temperature, degrees centigrade (calculated)
3. Top-oil temperature, degrees centigrade (calculated)
4. Top-oil temperature, degrees centigrade (test)
5. Ambient temperature, degrees centigrade
6. Average copper temperature at shutdown; 115 degrees centigrade by calculation, 117 degrees by test

Table II

Type	Bank	Name-Plate Rating (Kilovolt-Amperes)	Final Top-Oil Temperature (Degrees Centigrade)		Final Average Copper Temperature (Degrees Centigrade)	
			Calculated	Test	Calculated	Test
Self-cooled	1	3–10,000	108	93	113	109
Self-cooled	2	3–10,000	110	101	115	117
Self-cooled	3	3–10,000	107	101	112	113
Self-cooled	1	3– 5,000	85	78	118	113
Self-cooled	2	3– 5,000	80	73	111	107
Fan-cooled	2	3– 5,000	65	65	96	98

know the load readings, it is certainly during an emergency. Temporary test instruments can be used in some cases, but at important stations it is well to install a single high-scale ammeter for each transformer bank.

While all tests have been conducted at overload ratings equivalent to 24-hours operation at 120 degrees centigrade hot-spot temperature, the firm rating of bulk power stations has been based upon one week's operation at 112 degrees centigrade hot-spot temperature. More time is thus allowed for the replacement of a faulty unit in these stations. The transformers are large and not too easily transported. Some two hundred distribution and industrial-type substations with their smaller and more easily handled units are given a firm rating based on the 24-hours operation at 120 degrees centigrade hot-spot temperature.

Conclusions

Allowable emergency overloads for substation transformers, as determined by the method outlined, have been used for only a few years in establishing the firm ratings of substations, and thus no large amount of experience has yet been accumulated. While the maximum temperature limits selected may appear to be high compared with earlier practice, it does not follow that such limits always will be reached during an emergency. Rather, these limits have been set as ceilings that may be encountered only if an emergency occurs under the worst combination of circumstances.

If future experience indicates the wisdom of lowering these limits, that can be done easily without changing basically the method described. Meanwhile, and particularly during the war, the savings in critical materials and in investment made possible by the higher substation firm ratings, should more than offset any isolated cases of abnormal life consumption.

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Kilowatts, Kilovars, and System Investments

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WHAT portion of the total system investment can one equitably allocate to the system kilovar requirements? Or, looking at it in a little different way, what portion of the system kilowatt capacity is used by kilovars? The answer to these questions obviously depends upon the method of supplying the kilovars. It is indicated in this analysis that supplying the kilovar requirements of a system in general may be divorced from supplying the kilowatt requirements and that the two factors may be dealt with as independent commodities. This approach shows that maximum usage of system capacity is made in producing kilowatts.

This two-commodity concept has developed very rapidly during the past decade. Until the advent of the present-day capacitor, kilovars were largely supplied by kilowatt generators in the power plant and transmitted to the point of consumption. Synchronous condensers, by providing regulated kilovars, were used primarily for regulating high-voltage transmission lines. Smaller ones, however, have been used on occasions by central stations and industrials for supplying kilovars locally. It is on this basis that systems were designed and largely operated. The availability of a low-priced kilovar generator—called a capacitor—has contributed materially to the acceptance of the two-commodity philosophy. This concept will now be developed and its relation to our immediate objectives indicated.

An electric system is built and operated for the purposes of serving consumers with electric service. A consumer basically wishes—in addition to light, heat, and so forth—for his motor to spin and do work for him. It so happens that this service requires two types of electric energy. The major type, which is also the real pay load on a system, is known as active or kilowatt component.

It is this component that turns the lathes, grinds the flour, produces airplanes, and turns out the finished product for the consumer. The other type of energy known as reactive or kilovar component is the type that is needed to magnetize transformers and induction motors or any type of electric apparatus needing magnetic flux supplied by the power system in order to function. This latter type of energy does not cause the disk of a watt-hour meter to turn, but it can use system kilowatt capacity to generate it and deliver it to the consumer. The amount of system kilowatt capacity required to provide this kilovar requirement is obviously determined by the method of supplying it, which may be done in either of two ways or by a combination of them both:

1. By producing it in the kilowatt generators in power plants and transmitting it through the system to the point of consumption.

2. By producing it in auxiliary kilovar generators, such as capacitors, synchronous motors, and synchronous condensers, which generally are located at or near the load, and therefore, the problem of delivering them to where they are needed is solved.

An analysis of these two methods of supplying kilovars is given in the following section.

Basic Assumptions

Although several minor assumptions are used in the body of the paper as the occasion arises, the three basic ones given herewith obtain throughout:

1. That the total system investment may be segregated and allocated separately to generating equipment and to equipment having either voltage drop or current magnitude as the factor which determines its limiting output rating. Obviously a certain portion of the total investment may not fall logically into one of these three classifications, but it is felt that the error introduced by this assumption is small.

2. That steam and hydraulic turbine generator units can deliver their full kilovolt-ampere rating at unity power factor in a sufficiently large majority of the cases to permit its acceptance as a general fact, particularly as the error in this assumed fact will have a negligible effect on the conclusions.

3. That the progressively increasing kilovar requirement—the system I^2X—because of current flowing through the reactance of the system from the load to the generator be neglected. In other words, only the kilovar requirements of the load are used in plotting the data. This means that still more of the system investment really should be charged to the flow of kilovars when it is supplied by kilowatt generators. This assumption offsets to a large extent the errors introduced by assumptions 1 and 2, as this neglected component can amount to a large portion of the load requirements in particular instances.

General Conclusions

1. Kilowatts and kilovars can be dealt with most economically as two independent commodities using separate generators, in the general case. The amount of system facilities used by both in getting from generating source to load is determined by the economic balance between the added cost of the facilities and the increasing costs of lower-voltage smaller-size capacitor units required for connection adjacent to load.

2. A value of two to six per cent of the system investment is chargeable to the kilovar requirement when it is supplied near the load by auxiliary kilovar generators as contrasted to 20 to 25 per cent when supplied by kilowatt generators in the power stations for systems operating around 80 per cent power factor.

3. If rates are established for kilovars based on the cost of supplying them by kilowatt generators, they will provide an excessive inducement for the consumer to generate his own kilovars. The important system design and operating factor of kilovar control will then tend to pass out of the hands of the operating company into the hands of the consumer. This tendency is aggravated by each price reduction on kilovar generators, and their price today is only about 25 per cent of the 1929 level.

4. Approximately 330 pounds of critical materials are required to produce one kilovar when supplied by kilowatt generators and delivered through the system, as compared to five pounds (includes all mounting structures, housings, dielectric liquid, paper, and so forth) when supplied by capacitor kilovar generators near the load. Even dividing this ratio by a factor of 10 to allow for errors

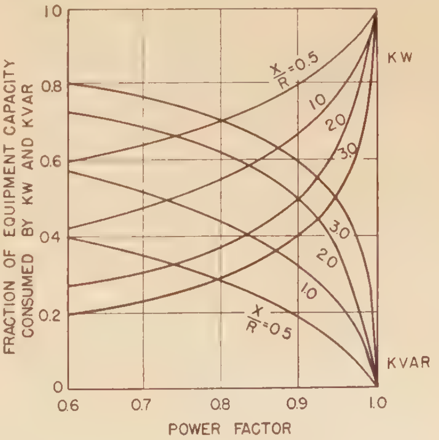

Figure 1. Allocation of system capacity usage when voltage drop is limiting factor

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in assumptions leaves a sufficiently large differential to substantiate the conclusion that kilovars in general should be produced by special generators near the load.

Analysis

I. Kilovars Supplied by Kilowatt Generators and Transmitted Through the System

Since a system can deliver its maximum pay load when operating at 1.0 power factor (if there is no stability limitation), this part of the discussion will be based on the viewpoint that the extent to which this maximum kilowatt capacity is reduced by the need for supplying kilovars determines the amount of system capacity consumed by the kilovar generation. With this premise, the equipment of a system naturally divides itself into three groups, as determined by the different ways in which kilovars use system kilowatt capacity, namely:

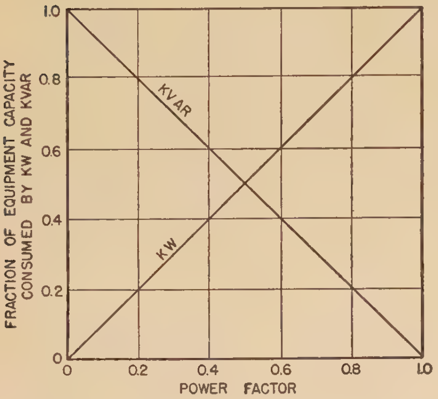

Figure 2. Allocation of system capacity usage when ampere capacity is limiting factor

1. Equipment such as overhead lines wherein voltage drop is the limiting factor.

2. Equipment such as transformers and cables wherein ampere capacity and therefore thermal loading are the limiting factor.

3. Generating equipment whose output is limited by thermal loading in either armature or field, depending upon the operating power factor and machine design.

1. *Where Voltage Drop Is Limiting Factor.* Equations can be derived which will give for any specified condition the fraction of the total drop caused by active and reactive current. On the basis that each component uses system capacity in proportion to its effect on voltage drop, these equations give directly the fraction of the total capacity in this type of equipment consumed by each. Such relations are shown plotted in Figure 1 as a function of power factor and the ratio of X/R of the circuit. These curves show that 30 to 70 per cent of the system voltage-limited capacity is consumed by the kilovars of an 80 per cent power-factor load in circuits having an X/R ratio of 0.5 to 3.0 respectively.

2. *Where Ampere Capacity Is Limiting Factor.* A piece of equipment such as a transformer, a regulator or a cable, having current magnitude as its limiting factor, can deliver full kilovolt-amperes at 1.0 power factor, that is, its full rating in kilowatts. At any other given power factor it can deliver only that fraction of its full kilowatt rating represented by the power factor; for example, at 0.8 power factor it can deliver 0.8 of its rating in kilowatts. Hence, for a given thermal (or ampere capacity), the portion of the capacity used by the active current is simply the per-unit power factor, and that portion used by the reactive current is unity minus the per-unit power factor. This division in the usage of the system capacity is on the basis of the kilowatt capacity used by each. These relations are shown plotted in Figure 2 for various power factors.

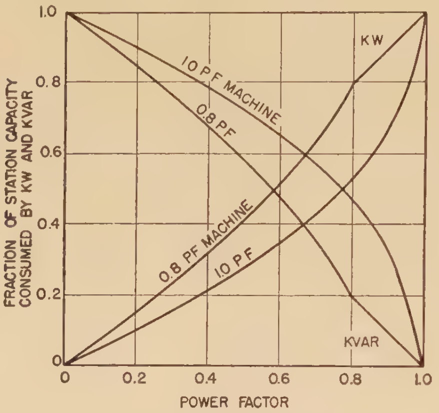

Figure 3. Allocation of steam-turbine-generator capacity usage

3. *Thermal Capacity of Generators.* This case is somewhat complicated by the fact that a generator is designed for operation at a predetermined power factor, and by the limitations of the prime movers, cooling water, steam facilities, and so forth. However, a frequent practice in respect to turbine-generator ratings is to install 0.8 power-factor generators with turbines good for 5/4 generator name-plate kilowatt capacity. This procedure permits 125 per cent name-plate kilowatt output to be delivered at 1.0 power factor. For several years it has been the practice to furnish 0.8 power-factor turbogenerators to utilities on this basis unless requested to the contrary, and also many utilities have specifically required units so rated in the interests of obtaining peak capacity. Therefore, it is felt that one reasonably may assume that a fair portion of the units in service today will deliver full kilovolt-amperes at 1.0 power factor and could be so operated if other sources for kilovars were made available. Some units admittedly have limitations such as end structure heating, when operating at increased power factor, and therefore each case should be checked specifically in determining its limits in this respect.

That this philosophy of operation and the possibilities ascribed to it in the preceding paragraph are reasonable is supported by the following quotation:[1]

> "Table I shows the present turbine-generator capacities as of December 8, 1941, totaling 919,550 kw, which is greater than the original kilowatt ratings by 20 per cent, primarily because of increases in power-factor ratings."

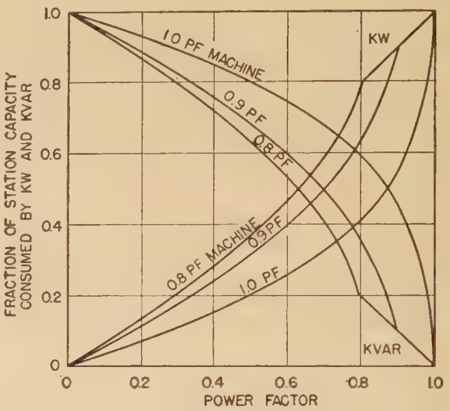

Figure 4. Allocation of hydroelectric-turbin generator capacity usage

This paper is a report on the kilovar-supply program of the Public Service Electric and Gas Company of New Jersey.

The above discussion also applies to modern 0.8 or 0.9 power-factor hydroelectric units, and so for the purposes of this discussion, the capacity of the station in kilowatts will be taken as the station rating at 1.0 power factor. The kilowatt capacity reduction from this base rating caused by the flow of kilovars will be allocated to the kilovars flowing.

Representative data for steam and hydroelectric generators, giving their outputs at reduced power-factor loadings, were studied from which the kilowatt output for various power factors and hence the reductions from base kilowatt rating were determined. These results are shown plotted in Figures 3 and 4.

These data present a reasonable allocation of the portion of each class of equipment capacity actually being used by active and reactive kilovolt-amperes during peak load conditions for various power factors. It is interesting now to speculate as to what portion of the total system investment is used—within these assumptions—in serving kilovars for different power factors in a few chosen systems.

Table I is a summary of the classifications of system investments for various types of equipment as compiled by the Federal Power Commission in 1939, for three representative systems. From these data the fraction of the total system investment in each type of equipment considered in this study may be tabulated.

Weighting these percentages by the amounts of each type of equipment that is consumed by kilovars at various power factors as shown in the data of Figures 1 to 4, one can determine what fraction of the total system investment is being used by reactive component for the three types of systems listed in Table I. Part I of the appendix develops the method for consolidating these data.

Table I. Classification of System Investments

	Company A	Company B	Company C
Steam	0.104	0.335	0.452
Hydroelectric	0.334	0.011	0
Transmission (current limit)	0.099	0.169	0.005
Transmission (voltage-drop limit)	0.134	0.039	0
Distribution (current limit)	0.203	0.293	0.526
Distribution (voltage-drop limit)	0.126	0.153	0.017
Total	1.00	1.00	1.00

Data taken from book compiled by Federal Power Commission, 1939, "Statistics of the Electric Utilities in the United States."

Consolidating these figures as suggested in the foregoing and assuming an over-all figure for X/R of 1.0 for voltage-limited equipment and 0.8 power-factor generators, the interesting data on Figure 5 are obtained. The results are surprisingly similar for the three rather different types of systems chosen. These curves indicate that 20 to 25 per cent of the system investment can be charged reasonably to the kilovar requirements when kilovars are supplied from generators operating at 0.8 power factor.

II. Kilovars Supplied by Auxiliary Kilovar Generators at or Near the Load

Kilovar generators can always be located any place on any system at essentially a constant cost per kilovolt-ampere of something less than ten dollars. Since these generators cannot always be placed adjacent to the requirement, some system capacity should be charged against them so a figure of \$15 will be added, making \$25 as an unquestionable outside cost of kilovar generators. Numerically, the use of separate kilovar generators means that a much smaller fraction of the total system investment is chargeable to reactive kilovolt-amperes. Since the kilowatt investment in a system may readily vary around \$400, the fraction of total system investment chargeable to kilovar—for correcting the loads to 1.0 power factor—over a range of system values—has been calculated and is shown plotted in Figure 6.

Figures 5 and 6 give at a glance the wide variation in the fraction of the total system investment chargeable to kilovar requirements for the two concepts. A value of two to six per cent of the system investment is chargeable to the reactive component when supplied near the load, as contrasted to 20 to 25 per cent when supplied by the kilowatt generators in the power stations for system loads around 80 per cent power factor.

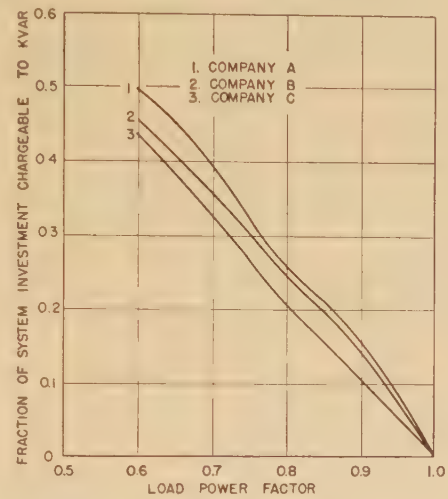

Figure 5. Fraction of system investment chargeable to kilovars when they are supplied by kilowatt generators and transmitted through the system to the load

Discussion

1. General

These figures indicate that a tremendous amount of relatively high-cost system kilowatt capacity is used by kilovars when it is supplied at a point distant from the load—presumably by kilowatt generators. This is a well-known and long-established fact as evidenced by the following extracts taken from the *Proceedings* of the 13th convention of National Electric Light Association, (held in Washington, D. C., June 1907) pages 81–83.

> "With the rapid increase in the industrial use of induction motors and in arc lighting by alternating current, the troubles incident to low power factor, in systems not designed for it, have become unfortunately familiar."

> "Low power factor has two serious results: It limits the capacity of the electrical part of the system by loading it up with unproductive current—current for which no revenue is obtained; and it means poor voltage regulation."

> "The rating of electrical apparatus depends primarily on the heating produced by the load, and this heating depends entirely on the current. The kilowatt load, therefore, unless the corresponding power factor is taken into account, is not a measure of the actual load on the apparatus."

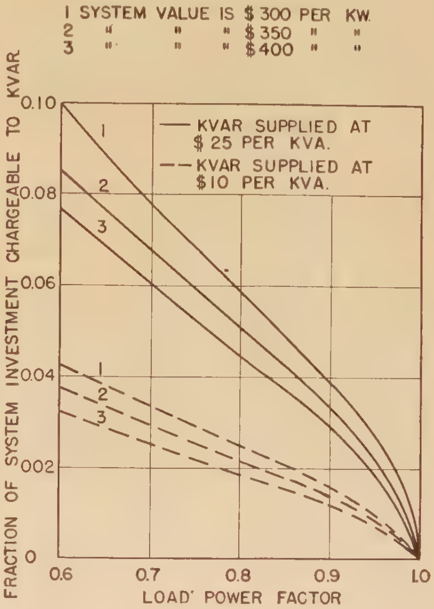

Figure 6. Fraction of system investment chargeable to kilovars when complete load kilovar requirement is supplied at or near the load

> "The second injurious effect of low power factor is poor voltage regulation. This effect is well known and requires no extended explanation."

These facts, however, although they have been known since the turn of the century, are just beginning to exert a real influence on the design and operation of power systems. Until the present period expansions were made beyond the immediate requirements to allow for normal expected growth and to cover new areas or to give necessary reliability. These practices provided margins therefore that could—for some time—be used by the kilovar requirements without impairing the kilowatt capacity.

The developments in the distribution field, such as load-center and unit-substation equipments and the advent of the low-priced kilovar generator—the capacitor—have changed this picture. Conditions are different now and should be so recognized. The expansion era is tapering off, and the war effort in particular is requiring that we get the most out of every bit of system capacity. The "injurious effects of low power factor" referred to in 1907 need not now be felt by the relatively high-cost kilowatt investment equipment. As outlined, the cost of kilovar generators is such that they can now be installed any place on a system at approximately ten dollars per kilovolt-ampere. In some cases the reactive component cannot be supplied right at the load, and so some additional system-investment allocation is required, but taking an outside figure of $25 per kilovolt-ampere still leaves plenty of margin to show the fallacy of attempting to use the system for transmitting kilovars to the loads from the generating stations.

2. Relation to the Immediate Objectives

Looking at the situation from a critical-material standpoint, the picture is about as follows: Assuming approximately $400 per kilowatt of capacity for power-system investment, and 25 per cent of this figure—or $100 as previously developed—as being allocated to the kilovar requirement at 0.8 power factor when it is furnished by the system as a whole, and further estimating the power system as averaging 50 cents per pound of electric equipment would mean approximately 200 pounds of equipment to provide 0.6 kilovar or approximately 330 pounds per kilovar. Contrast this with one kilovar being supplied by capacitors with approximately five pounds of material at most, including mounting, housings, dielectric liquid, paper, and so forth. We wondered why Japan has for several years installed capacitors in very large banks, such as one installation having 100,000 kva at a single location.[2] The answer is obvious when vital materials are really needed and are being used for other purposes, as apparently they were.

Our immediate concern in the war effort is to make available the maximum kilowatts from existing facilities. The possibilities in this respect through the use of auxiliary kilovar generators are readily apparent, and in many locations advantage is being taken of the indicated gains.[1–3] But it is generally accepted that still a majority of the kilovar requirements of the country are being supplied by kilowatt generators, which means valuable system generation, transmission, and substation capacity is being used by the reactive component that could be used in producing kilowatts. The relation between these facts and our immediate objectives is considered sufficiently self-evident to require no further amplification.

3. Effect on System Design and Operation During Normal Times.

If a system is set up and operated on the basis of supplying the bulk of its reactive requirements from the power generators, and rates are instituted accordingly in an endeavor to encourage the consumer to generate his own kilovars, several interesting angles present themselves. System kilowatt investment costs are relatively stable, and so the cost of delivering kilovars by the system as a whole remains substantially constant, but at a high level as indicated by Figure 5. With rates established for kilovars based on these costs, it would be very profitable obviously for a consumer to buy kilovar generators for approximately ten dollars per kilovolt-ampere and thus be relieved of buying the expensive product from the power company.

It is well known that parallel operation of many electric plants presents problems not common to the parallel operation of two or three plants. These problems are greatly magnified when part of the parallel plants do not co-operate in a manner conducive to economy, ease of operation, and continuity of service of the system as a whole. The kilovar generators—and sometimes large ones—operating on the consumer's premises, are really parallel plants, over which the operating company has little or no control. And these plants are there, in principle, because the consumer could not afford to buy the expensive kilovars offered by the operating company. Further, as the price of kilovar generators such as capacitors goes down, more and more of them will be installed by the consumers. These may or may not be regulated reactive installations. Since economic system design and operation are so intimately related to kilovar requirements, it follows that the operating companies would lose control of this important factor.[4] It is well known that even today—when load factors are a maximum—it is the light load periods that really give voltage and even stability troubles through lack of sufficient reactive control, and so what it would mean in this respect if the price of capacitors were greatly reduced in the future can be imagined easily. In fact, system design and operation could become subject uncomfortably to the mercy of the consumer and the price of capacitors instead of remaining in the hands of the operating company.

From this viewpoint it appears that the two requirements of a consumer—namely, kilowatts and kilovars—may be supplied best by the power company. The relatively long-term basis on which the utilities are financed, in view of their stabilized nature, allowing them to accept a slightly lower return on their investment as compared to industrials, together with the fact that the utility has greater latitude in arrangement of capacitors because of diversity to obtain best efficiency, seems to bear out such an approach.

This is a somewhat different philosophy than has prevailed generally within the operating companies, in that kilovars have been looked upon as a nuisance, and the companies were willing to pay the consumer a certain price to keep the kilovars off their systems.

The two requirements are definite, and from the point of view developed the two requirements may best be dealt with as two independent commodities, using separate "generators" and possibly little system equipment in common. This suggests their metering may even be done separately. The confusing terms power-factor and kilovolt-amperes might even be abandoned to advantage. The large cost-to-supply ratio of kilowatts to kilovars—the two units which go to make up power factor and kilovolt-ampere—logically indicates divorcing them. This large cost ratio further indicates that kilovolt-ampere metering, which gives equal weight to kilovars and kilowatts, is somewhat inequitable. It appears that with this philosophy, that is, the operating company providing the kilovar requirements, the utility would be in the business of selling *complete* electric service, in terms of both kilowatts and kilovars, and in addition, of having *complete* control of system design and operation, as it should have—resulting in maximum economy and usage of materials that go into making up a system.

Appendix. Allocation of System Capacity to Active and Reactive Uses

I. Kilovars Supplied by Kilowatt Generators and Transmitted to the Load

1. Where Voltage Drop Is Limiting Factor

The expression for voltage drop

$V_D = I(R\cos\theta + X\sin\theta)$

gives an accurate relation between voltage drop, current magnitude, power-factor angle, and the impedance of the circuit. For any specified condition, the fraction of the total drop caused by the active current is

$$V_P=\frac{R\cos\theta}{R\cos\theta+X\sin\theta}=\frac{\cos\theta}{\cos\theta+N\sin\theta}$$

and the fraction of the drop caused by the reactive current is

$$V_Q=\frac{N\sin\theta}{\cos\theta+N\sin\theta}$$

where N = ratio of reactance to resistance

θ = power-factor angle

On the basis that each component (active and reactive) consumes system capacity in proportion to its effect on voltage drop, the factors V_P and V_Q give directly the fraction of the total capacity in this type of equipment consumed by each, as a function of θ and N.

These relations are shown plotted in Figure 1 for power factors of 0.6 to 1.0 and for N's of 0.5, 1.0, 2.0, and 3.0.

An exact analysis may require an integrated value of V_P and V_Q for a particular system because of the variation in N. For example, one set of factors may be necessary for the transmission system, another for the distribution system.

2. Where Ampere Capacity Is Limiting Factor

For a given thermal limit (or ampere capacity) the decimal fraction of the capacity used by active current is

$$C_P=\cos\theta$$

and that used by the reactive is

$$C_Q=1-\cos\theta$$

This division of system capacity, or allocation of capacity consumed, is on the basis of the kilowatt capacity used by each. These relations are shown plotted in Figure 2, for various power factors.

3. Where Thermal Capacity in Generators Is Limiting Factor

Representative data for steam and hydroelectric generators, relative to their outputs at reduced power-factor loadings, were obtained, from which data for the kilowatt output for various power factors were determined. Letting K_P denote the fraction of maximum kilowatt output at reduced power factor, it follows that the fraction of station kilowatt capacity consumed by the reactive component at corresponding power factors is $1-K_P$, or

$$K_Q=1-K_P$$

The values of K_P and K_Q calculated from the data are shown plotted in Figures 3 and 4 respectively.

4. Combining the Three Types of Equipment

On the basis that active and reactive kilovolt-ampere requirements be allocated their share of the total system investment in proportion to their use of the system capacity, the relations derived above give directly the per-unit allocations for each particular type of equipment. And the fraction of the *total* system investment chargeable to the active and reactive components at any given power factor can be determined by evaluating T_P and T_Q as follows:

$$T_P=k_1V_P+k_2C_P+k_3K_P$$

= fraction of total investment chargeable to kilowatt load

$$T_Q=k_1V_Q+k_2C_Q+k_3K_Q$$

= fraction of total investment chargeable to kilovar load

where

k_1 = ratio of equipment investment having voltage drop as limiting factor to total system investment

k_2 = ratio of equipment investment having ampere capacity as limiting factor to total system investment

k_3 = ratio of generating-station investment to total investment

T = total system investment $=1.0=T_P+T_Q$

and V_P, V_Q, C_P, C_Q, K_P, K_Q are derived and defined above.

II. Kilovars Supplied by Auxiliary Kilovar Generators at or Near the Loads

The fractions of the system investment chargeable to the active and reactive components for this case are somewhat easier to develop, since the investments for each do not necessarily use common system equipment.

Therefore

$$T_P=\frac{\text{kilowatt investment}}{\text{total investment}}$$

$$T_Q=\frac{\text{kilovar investment}}{\text{total investment}}$$

where total investment is equal to the kilowatt investment plus the kilovar investment. Designating the kilowatt investment as 1.0, and, if sufficient kilovar is supplied always to improve the load power factor to 1.0, so that all the kilowatt capacity is available, the kilovar investment, as a fraction of the kilowatt investment is

$$\text{kilovar investment}=\frac{\sin\theta\text{ cost per kilovar}}{\cos\theta\text{ cost per kilowatt}}$$

$$=\tan\theta\frac{(\text{cost per kilovar})}{(\text{cost per kilowatt})}$$

Hence

$$T_P=\frac{1.0}{1.0+\tan\theta\dfrac{(\text{cost per kilovar})}{(\text{cost per kilowatt})}}$$

$$T_Q=\frac{\tan\theta\dfrac{(\text{cost per kilovar})}{(\text{cost per kilowatt})}}{1+\tan\theta\dfrac{(\text{cost per kilovar})}{(\text{cost per kilowatt})}}$$

and

$$T_P+T_Q=1.0=\text{total investment}$$

These relations are shown plotted in Figure 6 for system costs per kilowatt of \$300, \$350, and \$400 and for kilovar costs of \$10 and \$25, supplied near the load, to improve power factor of load to 1.0.

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Pulling Loads on Single- and Multiple-Conductor Impregnated-Paper Lead-Encased Cable, Solid Type

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LACK of factual information relative to the permissible pulling loads to which solid-type impregnated-paper lead-sheath power cable might be subjected by means of woven-wire pulling grips without damage to the sheath prompted the Insulated Power Cable Engineers Association to consider the subject and resulted in the laboratory tests described in this paper.

There seems to have been very little work done in recent years on this or related subjects that has appeared in the technical literature. Considerable authentic and valuable information is available relative to work done by the National Electric Light Association underground systems committee on cable installation. For convenience of reference some excerpts from literature have been included in Appendix I.

Because of a lack of field records or laboratory test results, it was considered desirable to carry out a test program involving a rather broad selection of samples with respect to conductor size and shape, thickness of insulation, and constructions that would be representative of the cable industry's production. All paper-cable manufacturers' products were represented in the selection of samples tested.

The question of permissible strain to which lead-sheathed oil-filled cable might be subjected without serious damage to the sheath from gouging had been before a committee of manufacturers' representatives since December 1936. The original suggestion, based upon the use of pulling eyes soldered to conductors and sheath and the results of field experience rather than laboratory tests, limited the pulling strain to a maximum of 5,000 pounds. This limitation was for the purpose of controlling the damage to the lead sheath by gouging to a depth of not more than 20 mils. A later recommendation was made to limit the pulling strain on single-conductor cable to 6,000 pounds and on three-conductor cable to 5,000 pounds.

By observing these values for maximum strain, it was thought the limit of sheath gouging could be reduced from 20 mils to 15 mils. This assumed adequate duct clearance, good duct construction clear of any harmful foreign matter, and good installation practice.

Conductor areas of oil-filled cable and of solid-type cable are sufficiently large in most cases to withstand pulling stress considerably in excess of the foregoing limitations without physical changes in the cable that will effect its electrical characteristics appreciably. If, in these cases, the maximum permissible pulling load on a cable is considered relative to the maximum permissible stress on conductor area, the tendency is to lose sight of the premise: that is, protection of the sheath against excessive gouging.

Conductor areas in solid-type paper cable are frequently insufficient for a pulling load of 5,000 or 6,000 pounds. In these cases it is necessary to limit the load to some lower value based upon a maximum permissible stress on the conductor or upon a combination of the conductor and sheath if pulling eyes are used.

If cable is pulled by means of woven-wire grips, the strength of the conductor is not involved unless the ends of the conductor are embedded into the sealing caps. Ordinarily this is not done, and the entire load is taken by the cable sheath.

A few simple rules that are easy for application in the field for the guidance of men charged with the safe handling of cable during installation with woven-wire grips are much to be desired. This approach to the problem, however, would broaden the consideration under the original premise: that is, of permissible stress on the cable sheath. There are many variable factors involved in the pulling of cable that are peculiar to particular installations. These factors cannot be contemplated under simple rules nor are they considered in connection with the simple formulas for maximum pulling stress and maximum pulling length proposed as a result of this investigation.

Table I. Sheath Dimensions, Ultimate Load, and Tensile Strength of Lead Sheaths

Sample	Measured Outside Diameter	Nominal Thickness of Sheath (By Manufacturers) Fraction	Nominal Thickness of Sheath (By Manufacturers) Decimal Equivalent	Measured Average Thickness Sheath	Calculated Area (Square Inches)	Load at Failure (Pounds)	Calculated Tensile Strength (Pounds Per Square Inch)
A. Single Conductor With Thin Wall Insulation Up to 5/32 Inch							
A-1 Specimen 1	1.21	6/64	0.094	0.115	0.3956	1,400	3,539
A-1 Specimen 2	1.18	6/64	0.094	0.1155	0.3863	1,200	3,106
A-3	1.25	1/8	0.125	0.087	0.3179	1,000	3,146
A-4	1.75	10/64	0.156	0.129	0.6569	2,410	3,669
B. Single Conductor With Heavy Wall Insulation Above 8/32 Inch							
B-1	1.25	7/64	0.109	0.102	0.3679	1,600	4,349
B-2	2.13	0.117	0.117	0.124	0.7815	1,930	2,470
B-3	1.18			0.090	0.3082	800	2,596
B-4	2.68			0.145	1.1548	2,500	2,165
C. Three Conductors With Thin Wall Insulation Up to 15 Kv							
C-1	1.73	7/64	0.109	0.124	0.6256	2,080	3,325
C-2	2.42	8/64	0.125	0.135	0.9691	2,750	2,838
C-3	2.22	9/64	0.141	0.127	0.8351	2,000	2,395
C-4	2.07	8/64	0.125	0.134	0.8150	2,500	3,067
C-5	1.29	6/64	0.094	0.105	0.3909	1,180	3,019
C-6	1.78	7/64	0.109	0.118	0.6161	1,820	2,954
D. Three Conductors With Heavy Wall Insulation 15 Kv and Above							
1	2.54	1/8	0.125	0.131	0.9910	2,500	2,523
2	2.56	1/8	0.125	0.142	1.0787	2,500	2,318
D-3	2.89	9/64	0.141	0.153	1.3156	4,450	3,382
D-4	2.27	1/8	0.125	0.136	0.9118	2,460	2,698
5	2.50	1/8	0.125	0.134	0.9960	2,500	2,510
D-5	3.22	9/64	0.141	0.158	1.5199	3,700	2,434
6	3.12	9/64	0.141	0.152	1.4173	4,500	3,175

The tensile strength of lead sheath was calculated on the basis of the tape-measured outside diameter near the end of the sample and the average measured thickness of lead sheath obtained from rings of sheath cut just back of the end seals subsequent to the pulling test. The sheath had not been damaged or stretched at these points because of protection by the reinforcing shields on the pulling grip.

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Table IA. Specimen Test Data

Sample *D*-3

Three-Conductor 800,000-Circular Mil Type-*H*, $^{13}/_{64}$ Paper, $^{9}/_{64}$ Lead

(*a*) Grips—Kellems *P* 250

(*b*) Gauge Length—20 Inches

(c) Fracture at 4,450 Pounds, $13^{1}/_{2}$ Inches Within Bottom Grip, 2 Feet $1^{1}/_{2}$ Inches From Bottom; Spiral in Form

Load (Pounds)	Interval (Minutes)	Lead Sheath Elongation (Inches Per Inch)	Vacuum Inches of Mercury		Outside Diameter (Inches)	
			Top	Bottom	Top	Bottom
500		0.0000	$^{1}/_{8}$	$1^{1}/_{8}$*	2.90	2.90
1,000		0.0010	$^{5}/_{8}$	$1^{3}/_{4}$*	2.90	2.90
	1	0.0015	$^{5}/_{8}$	2 *	2.90	2.89
	2	0.0015	$^{3}/_{4}$	$2^{1}/_{8}$*	2.90	2.89
	3	0.0015	$^{3}/_{4}$	$2^{1}/_{4}$*	2.90	2.89
	4	0.0015	$^{7}/_{8}$	$2^{3}/_{8}$*	2.89	2.89
	5	0.0015	1	$2^{3}/_{8}$*	2.90	2.89
1,500		0.0015	$1^{3}/_{8}$	2 *	2.89	2.89
	1	0.0015	$1^{1}/_{2}$	$2^{1}/_{4}$*	2.90	2.89
	2	0.0015	$1^{1}/_{2}$	$2^{1}/_{4}$*	2.90	2.90
	3	0.0015	$1^{1}/_{2}$	$2^{3}/_{8}$*	2.89	2.90
	4	0.0015	$1^{5}/_{8}$	$2^{1}/_{2}$*	2.90	2.90
	5	0.0015	$1^{5}/_{8}$	$2^{1}/_{2}$*	2.89	2.90
2,000		0.0015	$2^{3}/_{8}$	$1^{1}/_{8}$*	2.89	2.90
	1	0.0025	$2^{5}/_{8}$	$1^{1}/_{8}$*	2.89	2.88
	2	0.0030	$2^{5}/_{8}$	$1^{1}/_{8}$*	2.89	2.88
	3	0.0030	$2^{5}/_{8}$	$1^{1}/_{4}$*	2.89	2.90
	4	0.0030	$2^{5}/_{8}$	$1^{3}/_{8}$*	2.89	2.89
	5	0.0030	$2^{5}/_{8}$	$1^{1}/_{2}$*	2.89	2.88
2,500		0.0030	$4^{1}/_{8}$	$^{7}/_{8}$**	2.90	2.90
	1	0.0030	$4^{1}/_{4}$	$^{7}/_{8}$**	2.89	2.90
	2	0.0035	$4^{1}/_{4}$	$^{3}/_{4}$**	2.90	2.88
	3	0.0035	$4^{1}/_{4}$	$^{5}/_{8}$**	2.89	2.90
	4	0.0035	$4^{1}/_{4}$	$^{1}/_{2}$**	2.90	2.90
	5	0.0035	$4^{1}/_{4}$	$^{3}/_{8}$**	2.89	2.89
3,000		0.0075	$7^{3}/_{4}$	4 **	2.89	2.89
	1	0.0100	9	$4^{3}/_{8}$**	2.89	2.89
	2	0.0100	$9^{1}/_{4}$	$4^{7}/_{8}$**	2.89	2.89
	3	0.0100	$9^{3}/_{8}$	5 **	2.88	2.89
	4	0.0100	$9^{1}/_{2}$	$5^{1}/_{8}$**	2.89	2.88
	5	0.0100	$9^{5}/_{8}$	$5^{1}/_{4}$**	2.89	2.88
3,500		0.0185	$17^{1}/_{2}$	$11^{5}/_{8}$**	2.88	2.87
	1	0.0250	$19^{1}/_{2}$	$13^{1}/_{2}$**	2.88	2.87
	2	0.0265	21	$14^{1}/_{4}$**	2.88	2.87
	3	0.0300	$22^{1}/_{8}$	$14^{5}/_{8}$**	2.88	2.87
	4	0.0320	$22^{1}/_{4}$	$14^{7}/_{8}$**	2.88	2.87
	5	0.0320	$22^{1}/_{4}$	15	2.87	2.87
4,000		0.0410	24	$23^{1}/_{4}$**	2.87	2.86
	1	0.0435	$25^{1}/_{4}$	$23^{7}/_{8}$**	2.87	2.85
	2	0.0450	$25^{7}/_{8}$	24 **	2.87	2.85
	3	0.0450	26	$24^{1}/_{2}$**	2.87	2.85
	4	0.0450	26	$24^{1}/_{2}$**	2.87	2.85
	5	0.0470	26	$24^{1}/_{2}$**	2.85	2.83
4,450		0.0525	$26^{1}/_{4}$	$26^{7}/_{8}$**	2.85	2.81

Extension of lead sheath beyond end of copper conductors: Top—$4^{1}/_{2}$ inches; bottom—9 inches.

* Positive pressure.

** Negative pressure.

Table II. Comparison of Tensile-Stress Values Under the Respective Classifications for Cable Construction

	Pounds Per Square Inch		
Group A. Single Conductor With Thin Wall Insulation up to $^{5}/_{32}$ Inch			
A-1 Specimen 1	3,539	Average	3,365
A-1 Specimen 2	3,106	Minimum	3,106
A-3	3,146	Maximum	3,669
A-4	3,669		
Group B. Single Conductor With Heavy Wall Insulation Above $^{8}/_{32}$ Inch			
B-1	4,349	Average	2,895
B-2	2,470	Minimum	2,165
B-3	2,596	Maximum	4,349
B-4	2,165		

Group C. Three Conductors With Thin Wall Insulation Up to 15 Kv—Comparison Between Belted and Type-H Cables	Belted	Type H
C-1	3,325	
C-2	2,838	
C-3	2,395	
C-4	3,067	
C-5	3,019	
C-6		2,954
Average	2,929	
Minimum	2,395	
Maximum	3,325	

Group D. Three Conductors With Heavy Wall Insulation 15 Kv and Above	Belted	Type H
1	2,523	
2	2,318	
D-3		3,382
D-4	2,698	
5		2,510
D-5		2,434
6		3,175
Average	2,513	2,875
Minimum	2,318	2,434
Maximum	2,698	3,382

The recommendations of a committee which were before the Insulated Power Cable Engineers Association for consideration and approval at the time this test program was decided upon were:

"In the case of cables in which the pulling eye is attached to the end of the cable, the maximum pulling strain shall be based on 0.008 pound, per circular mil of copper with a maximum of 6,000 pounds.

"Where a cable grip is used over the lead sheath for pulling, the maximum pulling strain shall be taken as 1,500 pounds, for each one-inch diameter of cable. For example: A three-inch diameter of cable would call for 4,500 pounds. This suggestion does not take into consideration what is inside the lead sheath; that is, we may have large copper and light walls of insulation or small copper and heavy walls of insulation for a given diameter.

"These pulling strains are based on a maximum of 750-foot pull with no bends. Where bends occur, it has been suggested that the permissible pulling strain be reduced ten per cent for each bend."

The proposed rule applicable to cable fitted with pulling eyes received rather general acceptance. However, there were expressions of preference for the 5,000-pound limit, particularly with respect to single-conductor cable.

The proposed rule applicable to cable pulled with a woven-wire grip was judged unacceptable. It was considered preferable to base the permissible pull upon the area of the sheath and the permissible stress to which the sheath material could be subjected without damage from stretching. As a result of this investigation which undertakes to determine the ultimate stress values and extension of lead sheath, it appears that the maximum permissible pulling loads calculated by the foregoing proposed rule would be in excess of the strength of the cable sheath.

Test Specimens

It was directed that samples should be selected from the lists submitted by the manufacturers on the basis of their being as representative as possible as to conductor size under each of the following groupings:

Single-conductor cable:

A with thin wall of insulation up to $^{5}/_{32}$ inch.

B with heavy wall of insulation about $^{8}/_{32}$ inch and heavier.

Three-conductor cable:

C with thin wall insulation rated up to 15 kv.

D with heavy wall insulation rated at 15 kv. and over.

Classification of the samples selected

Table IIA. Tensile Strength of Lead Sheath From Table II

Group	Average	Minimum	Maximum
A	3,365	3,106	3,669
B	2,895	2,165	4,349
C Belted	2,929	2,395	3,325
D Belted	2,513	2,318	2.698
C Type *H*	2,954	One sample only	
D Type *H*	2,875	2,434	3,382

under the foregoing groupings, as well as dimensional characteristics, are shown in Table I.

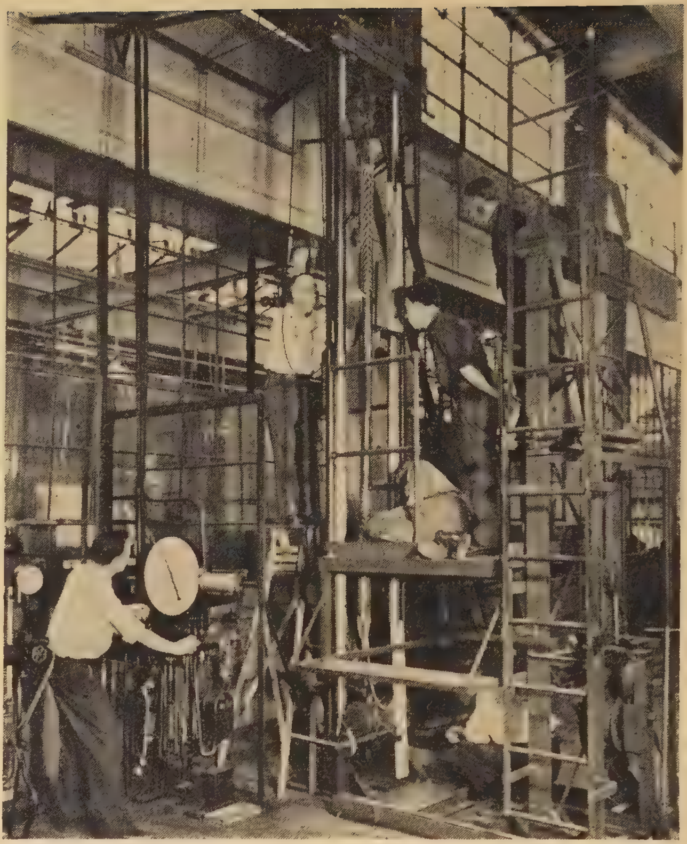
Figure 1. Specimen under test, showing primarily points of observation

Testing Equipment and Procedure

The pulling grips used were furnished by Kellems Company for all except a few preliminary samples. The manufacturers' statement relative to the design, selection, and performance of pulling grips has been incorporated with this text as a matter having direct bearing upon this work and also because of its potential value in the selection of grips for application in the field.

Tests were conducted in the John A. Roebling's Sons Company laboratory. An Amsler testing machine of 40,000 pounds capacity was used. Samples tested with pulling grips were under the 10,000 pound range, and samples fitted with pulling eyes were under the 40,000 pound range.

The original end seals were removed from the samples as received. These were replaced with new end seals, each with a bicycle-valve insert.

A *U*-tube mercury manometer was connected to the bicycle-valve stem at each end of the sample by means of a flexible rubber hose. The object of this attachment was to use the manometer as an indicator of the rate and extent to which vacuums would develop as the sheath stretched and pulled away from the end of the cable under increased load. Also, loss of vacuum was to be used as the criterion for failure of the sheath. The thought was that the end seals might not be tight, that the pinching action of the pulling grips may be responsible in some cases for opening up cable end seals, and that faults of this nature could not be detected by observation.

The manometers were connected to the valves in the end seals before any load was applied. Connection of the flexible rubber tube to some of the early samples tested produced a slight pressure indication on the manometers. A slight drainage of compound to the lower end of the sample when it was mounted vertically in the testing machine also produced slight positive pressure readings. As the sheath stretched, this pressure was dissipated, and vacuum developed. In these instances pressure is indicated on the test sheets by a plus sign. As the load was applied, the vacuum in inches of mercury was read to the nearest $^1/_{16}$ inch on each manometer scale.

Measurements of extension of sheath between grips were taken between 20-inch and 40-inch bench marks on the sheath, each mark being at least six inches from the ends of the pulling grips. The variation in distance between bench marks was due to the variation in the length of grips used. As long a gauge length as possible was used on all cables, at the same time using a gauge length that was a multiple of ten inches. The gauge points on the cable moved with reference to two fixed 18-inch scales mounted on the frame of the testing machine. The scales were graduated in hundredths of an inch. All readings on the scales were taken to the nearest $^1/_{100}$ of an inch.

The bottom head of the testing machine was stationary—the top head was movable. The bench marks on the sheath therefore moved upward; hence, the total elongation for any load was equal to the difference between the scale readings at that load and at the initial load. This divided by the gauge length gave the unit elongation in inches per inch. As the load was applied, the movement of these gauge points was determined to the nearest 0.01 inch, read on a steel scale.

Outside diameters of the cable were measured by diameter tape during tests. These were taken at the top and bottom bench marks on the cable sheath unless otherwise indicated.

For the originally planned tests the initial loads applied, as well as the subsequent load increases, were varied somewhat, depending upon the size of the cable. Each load value was held for an interval of five minutes. Sheath elongation and manometer readings were taken at each one-minute interval. The five-minute holding interval for each loading was chosen as a minimum time that a cable, of appreciable size and length fitted with a woven-wire grip, would be subjected to pulling stress during installation. This is estimated as approximately equivalent to pulling a 300-foot length of cable into a conduit at a rate of 60 feet per minute.

The ultimate strength of the sheath was taken as the maximum load indicated on the dial prior to failure.

Chemical analyses of the sheaths were not made. It was presumed that they represented a cross section of commercially pure leads.

All samples were tested at room temperature, approximately 75 degrees Fahrenheit.

The accompanying illustrations (Figures 1 to 4) show the method of mounting

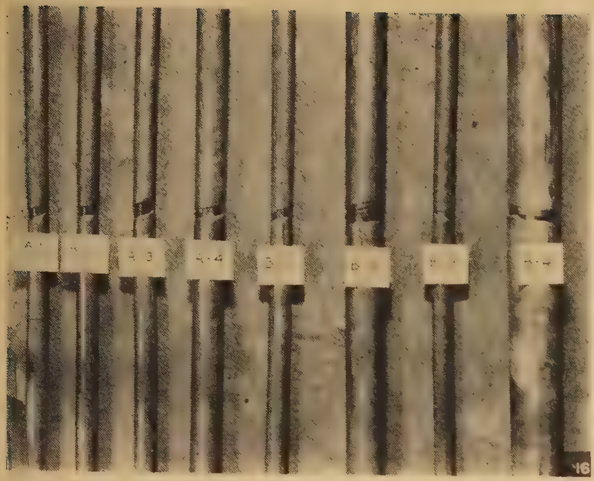

Figure 2. Fractures of cables *A*-1 to *B*-4 inclusive

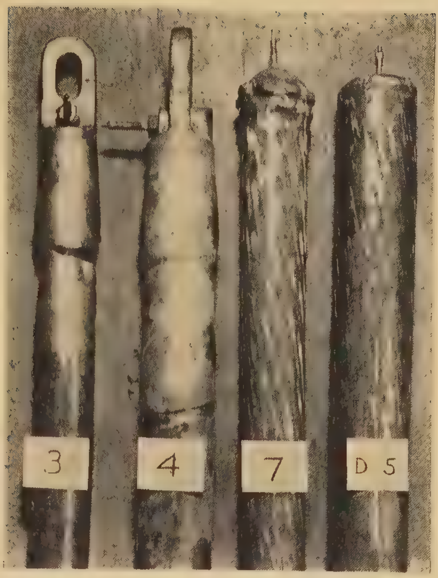

Figure 3. Samples showing typical valve inserts

the eight-foot samples, the type of testing equipment used, types of end terminations, the nature of the sheath fractures with both woven-wire grips and socket-type pulling eyes.

Pulling Grips

Experience with miscellaneous stock pulling grips demonstrated that the proper selection of size, type, and application of the grips to the ends of the cable was necessary if tests of the samples to destruction were to be realized.

The Kellems Company's representative was present to observe the performance of the pulling grips under load during the planned series of tests and contributed the following statement:

> Two types of grips were used, one the old type, constant weave, in which every mesh was exactly the same size as every other mesh in the grip. In other words, all wires were crossed at exactly the same angle throughout the length of the grip. The other type was woven with a variation in the size of the mesh; that is, the angle at which the wires were crossed was varied, decreasing gradually from the receiving end to the pulling end of the grip.
>
> It was found that the variable-mesh grip is much more effective than the constant-mesh grip, as the strain is evenly distributed over a greater area of the cable. In the constant-mesh grip, the strain is concentrated near the pulling end of the grip, and there is a tendency to localize the bite of the grip and cut into the sheath at this point.
>
> Also, the flexible variable-mesh grip held, whereas the constant-mesh stiff grip had to be served down with wire to the cable to keep it from slipping under heavy loading.
>
> As a result of these tests, certain improvements in design were made to prevent slippage and to improve distribution of strain. These improvements are now standard in current manufacture.
>
> In the past the tendency has been to use grips with too high a breaking strength; the strength of the grip is many times that of the cable sheath. When the grip is sufficiently strong but not far in excess of the cable sheath strength, the result is a more flexible grip and a more even distribution of the strain on the cable. This increases the effectiveness of the grip and also the holding power of the grip to the cable under it.
>
> There is a definite relationship between diameter and length of grip—the greater the diameter, the greater the length required. This is true only to a limited extent with the old-type constant-mesh grips, since the strain is concentrated near the pulling end of the grip. In the variable-mesh pulling grips this relationship between diameter and length has been taken into consideration, and larger diameter grips have been made proportionately longer. Throughout each set of cable size ranges, the length of the grip is increased with the load rating.

A specimen test-data sheet has been reproduced in Table I*A*. This shows how gradually the deformation of the sheath takes place under an increasing static load applied for the time intervals shown.

Stress-strain relations for a selection of samples representative of each classification of cable as given in Table I are shown in Figure 5.

Stress-vacuum relations for these same cables are shown in Figure 6. The shift in the relative position of the curves for the respective samples is due to using the average of the manometer readings from the two ends of the sample.

The summary of information pertaining to all but a few preliminary and special test samples is given in Table III which gives some idea of the dimensions and relative performance of the different cables. The tensile stress values from Table I are regrouped in Table II in order to get a comparison of average, maximum, and minimum values obtained for the types of cable under the respective classifications.

As a result of the work shown in Tables I, II, and III it is recommended that the values of permissible pulling load and the maximum length of lead-sheathed impregnated-paper cable solid type, that should be pulled with a woven-wire grip without damage to the cable, given in Table IV, be used as a first approximation.

Values given in Table IV and equation 1 are based upon a maximum permissible stress for commercially pure lead sheath of 1,500 pounds per square inch and a conduit friction coefficient of 0.4.

Comparable values to those given in Table IV for a cable of other sheath dimensions may be calculated by using the formula.

$$S_m = 4{,}712t(D-t) \text{ pounds} \qquad (1)$$

The recommended maximum permissible pulling length of a given cable may

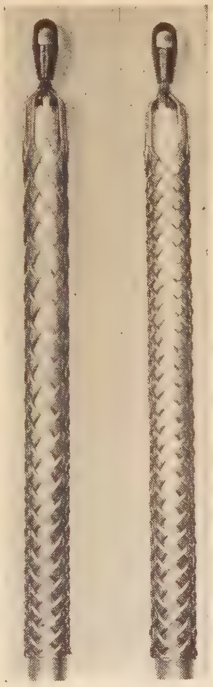

Figure 4. Kellems' pulling grips

Left—Variable mesh

Right—Constant mesh

Table III. Outside-Diameter Measurements Along Sample After Break

Diameter Tape—Inches

Distance From Bottom	A-1 Specimen 1	A-1 Specimen 2	A-3	A-4	B-1	B-2	B-3	B-4	C-1	C-2	C-3	C-4	C-5	C-6	D-3	D-4	D-5
0' 6"	1.210	1.215	1.255	1.735	1.245	2.20	1.19	2.70	1.745	2.42	2.21	2.09	1.29	1.80	2.90	2.27	3.24
1' 0"	1.210	1.210	1.250	1.730	1.255	2.205	1.20	2.71	1.745	2.415	2.22	2.07	1.285	1.795	2.89	2.265	3.235
1' 6"	1.205	1.205	1.235	1.705	1.255	2.185	1.195	2.695	1.735	2.405	2.205	2.07	1.28	1.795	2.865	2.26	3.21
2' 0"	1.180	1.125	1.225	1.635	1.225	2.15	1.185	2.685	1.735	2.37	2.20	2.035	1.255	1.77	2.805	2.255	3.185
2' 6"	1.160	1.160	1.225	1.695	1.235	2.095	1.17	2.66	Fracture	2.375	2.165	2.04	1.255	1.745	2.79	2.235	3.19
3' 0"	1.185	1.185	1.240	1.705	1.245	2.215	1.165	2.665	1.72	2.38	2.135	2.025	1.265	1.76	2.825	2.24	Fracture
3' 6"	1.195	1.195	1.245	1.725	1.25	2.115	1.165	2.635	1.725	2.39	2.125	2.05	1.28	1.77	2.855	2.245	3.195
4' 0"	1.205	1.200	1.255	1.725	1.245	2.105	1.175	2.67	1.735	2.395	2.15	2.05	1.28	1.78	2.875	2.25	3.185
4' 6"	1.200	1.200	1.255	1.735	1.25	2.125	1.17	2.67	1.735	2.395	2.14	2.055	1.28	1.80	2.885	2.23	3.20
5' 0"	1.200	1.195	1.245	1.74	1.25	2.145	1.18	2.67	1.72	2.385	2.14	2.04	1.28	1.79	2.90	2.22	3.21
5' 6"	1.190	1.180	1.240	1.735	1.24	2.135	1.18	2.685	1.72	2.38	2.125	2.025	1.28	1.775	2.885	2.20	3.19
6' 0"	1.185	1.170	1.225	1.725	1.24	2.115	1.175	2.665	1.70	2.37	2.165	2.015	1.275	1.775	2.84	2.175	3.185
6' 6"	1.130	1.145	1.210	1.705	1.205	2.105	1.17	2.655	1.72	2.335	2.195	2.00	1.265	1.77	2.785	2.26	3.20
7' 0"	1.200	1.150	1.250	1.69	1.235	2.10	1.175	2.59	1.725	2.405	2.20	2.025	1.265	1.755	2.85	2.275	3.215
7' 6"	1.210	1.205	1.255	1.72	1.25	2.105	1.175	2.625	1.735	2.41	2.20	2.06	1.245	1.73	2.885	2.265	3.225
8' 0"	1.210	1.210	1.260	1.745	1.25	2.13	1.185	2.68	1.74	2.42	2.20	2.075	1.22	1.73	2.895	2.27	3.245
8' 6"	1.210	1.215		1.745		2.16	1.175	2.70			2.21	2.085	1.27	1.78	2.885		3.24
9' 0"						2.17	1.195	2.715					1.285	1.79	2.885		
9' 6"						2.18		2.71					1.295	1.79			
10' 0"								2.52					1.30	1.795			
10' 6"								2.725									
Failed at	2' 2"	6' 8½"	6' 4"	2' 0"	6' 9"	2' 7½"	2' 5"	3' 1½"	2' 6"	6' 4½"	3' 3½"	6' 7½"	7' 9"	7' 8"	2' 1½"	5' 9"	3' 0"
Outside diameter*	1.120	1.120	1.195	1.630	1.170	2.055	1.155	2.52	1.66	2.32	2.125	1.985	1.230	1.720	2.775	2.150	3.13
Stress	3539	3106	3146	3669	4349	2470	2596	2165	3325	2838	2395	3067	3019	2954	3382	2698	2434
Elongation, inch per inch	0.0717	0.1220	0.0677	0.0577	0.0203	0.2037	0.0650	0.1057	0.0623	0.0837	0.1890	0.0875	0.0483	0.0662	0.0525	0.1335	0.0827
(a). Location of Sheath Failure Relative to End of Grip																	
Under																	
Upper grip		2"	4½"		10"		1½"†			5"	3"†	10"	3"	4½"		3"	
Lower grip	3"			6"		3"		1½"	3"						13½"		1"
(b). Approximate Weight of Cable—Pounds Per Foot																	
Approximately pounds per foot	3.2	3.2	2.8	6.7	3.7	5.2	2.4	9.9	5.6	10.8	11.0	6.7	2.6	4.8	15.4	8.6	14.8
(c). Vacuum Developed—Inches of Mercury																	
Top end	13.5	17.75	14.25	11.25	0.5	23.0	8.0	25.5	11.25	24.0	19.88	22.75	18.38	16.38	26.25	16.0	19.5
Bottom end	12.5	11.75	15.75	11.625	0.0	22.88	14.75	23.5	11.5	10.0	19.38	22.88	15.5	18.88	26.88	16.13	25.0

* Diameter tape measurements adjacent to the sheath failure. † Exception—failure outside the grip.

Table IV. Maximum Pulling Loads and Calculated Length of Paper Cable Permissible When Pulled With a Woven-Wire Grip

Core Diameter Range (Mils) Minimum	Core Diameter Range (Mils) Maximum	Nominal Thickness of Sheath (Inches)	Outside Diameter Range (Inches) D_1	Outside Diameter Range (Inches) D_2	Recommended Maximum Permissible Pulling Load (Pounds) P_1	Recommended Maximum Permissible Pulling Load (Pounds) P_2	Recommended Maximum Permissible Pulling Length (Feet) L_1	Recommended Maximum Permissible Pulling Length (Feet) L_2
251 –		0.078	0.407		120.9		302	
	– 600	0.078		0.756		249.2		623
601 –		0.086	0.773		278.4		696	
	– 900	0.086		1.072		399.6		999
901 –		0.094	1.089		440.8		1,102	
	–1,200	0.094		1.388		573.2		1,433
1,201 –		0.101	1.403		618.4		1,546	
	–1,500	0.101		1.702		761.9		1,905
1,501 –		0.109	1.719		827.0		2,068	
	–1,800	0.109		2.018		980.6		2,452
1,801 –		0.117	2.035		1,058.0		2,645	
	–2,100	0.117		2.334		1,222.0		3,055
2,101 –		0.125	2.351		1,311.0		3,278	
	–2,400	0.125		2.650		1,487.0		3,718
2,401 –		0.133	2.667		1,589.0		3,973	
	–2,700	0.133		2.966		1,776.0		4,440
2,701 –		0.141	2.983		1,889.0		4,723	
	–3,000	0.141		3.282		2,087.0		5,218
3,001 –		0.156	3.313		2,320.0		5,800	
	–3,300	0.156		3.612		2,541.0		6,353
3,301 –		0.164	3.629		2,678.0		6,695	
	–3,600	0.164		3.928		2,909.0		7,273
3,601 –		0.172	3.945		3,058.0		7,645	
	–3,900	0.172		4.244		3,300.0		8,250

be calculated on a comparable basis by using the formula

$$L_m = \frac{S_m}{cw} \text{ feet} \qquad (2)$$

The thickness t and outside diameter D of the cable sheath are in inches. The recommended maximum pulling load S_m is expressed in pounds, the weight of cable w is in pounds per foot, and the pulling length L_m in feet.

The use of these formulas requires but a limited amount of information including the weight per foot, outside diameter, and thickness of sheath, all of which should be available from the reel marking.

Interpolation of permissible pulling loads for standard thicknesses of sheath of intermediate diameters may be made from Table IV or taken from Figure 7.

The maximum recommended pulling-length values, Table IV are for cable having a weight of one pound per foot. These should be divided by the weight per foot of the cable to be pulled.

In instances where the use of a high-strength brand of commercially pure lead or an alloy sheath is required, a higher maximum stress value than 1,500 pounds per square inch could be justified.

A modification of the maximum permissible pulling length could be justified likewise, if facts are available as to prevailing coefficient of friction, duct condition, grade, bends, clearance, lubrication, and snubbing action to be expected at the entrance manhole.

Observations From Test Procedure and Results

None of the sheath failures under the original series of tests were between bench marks. With two exceptions the failures were under the grip and within ten inches of the back end. The two failures beyond the grips were $1^1/_2$ and 3 inches from the back end as shown in section (*a*) of Table III.

The recorded elongations do not represent the maximum strain values, because they do not involve that portion of the sheath including the failure.

Table VA

A. First Set

Section No. Bench Marks (Inches)	Top 1 10	2 10	3 20	4 10	5 20	6 10	Bottom 7 10
Sample A	Elongation in inches per inch at initial maximum stress of 2,180 pounds per square inch						
	000	0.013	0.066	0.037	0.054	0.012	000
	After 46 minutes failure occurred—ultimate elongations						
	000	0.036	0.180	0.088	0.098	0.030	000
	Failure occurred 3 inches within the back end of upper grip						
Sample B	Elongation in inches per inch at initial maximum stress of 2,400 pounds per square inch						
	000	0.018	0.111	0.076	0.044	0.018	0.002
	After 7 minutes failure occurred—ultimate elongations						
	0.001	0.015	0.129	0.097	0.096	0.019	0.003
	Failure occurred just below the back end of the upper grip						

Under the grip where the great majority of the failures occur, the resultant load is believed to be a combination of lateral compression and longitudinal tension in contrast to substantially tension stress only on that part of the sheath between the bench marks.

In general, the angularity of the sheath failure corresponded with that of the underlying paper insulation or shielding tape. This indicates a binding of the sheath to the core.

The paper tapes or the shielding tape and paper tape underlying the lead sheath at the point of failure showed appreciable change in mechanical uniformity. It is not known whether this change in position takes place gradually at these locations of greatest extension of the sheath or whether the change occurred when the sheath parted.

There did not seem to be serious abrasion of the sheath because of the grips, as slippage was practically eliminated in most cases.

Sample 7, Figure 3, which had the conductors soldered into the end seal, could not be tested to destruction, as the sheath bulged just back of the end seal which caused the grips to slip.

If cable end seals of usual design are properly made at the factory, there is little prospect of damaging them at any permissible stress to which the cable sheath should be subjected, especially if pulling grips of the reinforced type are used. There was but one instance where vacuum did not develop at the end of the sample.

Examination of the samples showed that there was appreciable coning of the

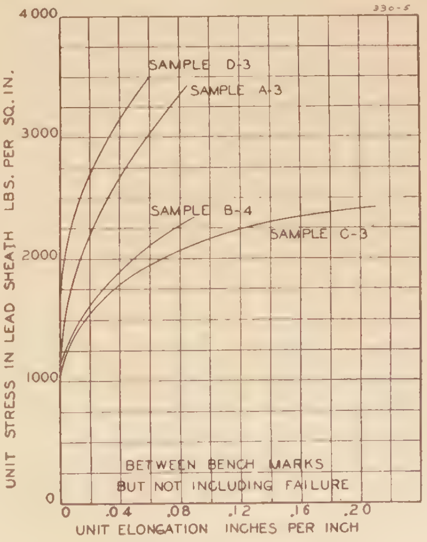

Figure 5. Stress strain relation—lead cable sheath

Table VB. Summary of Application of Load and Test Results

B. Second Set

	Specimen			
	8g	8i	8h	8f
Time of loading (seconds)	80	155	350	420
Time of loading (minutes)	1.33	2.58	5.83	7.0
Area of sheath (square inches)	1.586	1.609	1.604	1.641
Stress at failure (pounds per square inches)	3,000	2,870	2,760	2,570
Rate of stress application (pounds per square inch per minute)	2,250	1,110	473	367
Load at failure (pounds)	4,800	4,580	4,410	4,120
Rate of loading (pounds per minute)	3,600	1,775	757	588
Elongation 10 inches (inches per inch) measured at center of specimen	0.218	0.144	0.180	0.165
Vacuum (inches of mercury)				
Top end	−26	−29	−28½	−28½
Bottom end	−22¼	−24⅞	−24⅜	−19⅝
Outside diameter (inches)				
Before test	3.27	3.28	3.27	3.27
After test	3.20	3.23	3.22	3.20
Elongation 10-inch gauge length (inches per inch)				
Location along sample				
Top 1	0.000	0.000	0.000	0.000
2	0.030	0.015	0.010	0.031
3	0.190	0.130	0.080	0.136
4	0.215	0.145*	0.267*	0.173
5	0.210	0.142	0.180	0.180
6	0.295	0.145	0.187	0.244*
7	0.287*	0.080	0.112	0.037
8	0.053	0.050	0.015	0.000
Bottom 9	0.000	0.000	0.000	0.000
End displacement (inches)				
Top	5.4	7.7	7.2	0.9
Bottom	9.5	3.5	3.9	5.3

* Nearest to point of failure.

insulation off the end of the conductors of single-conductor cable, not so much on belted cable, and very little of this at the ends of type-*H* cable.

A maintained load of 1,000 pounds per square inch of sheath causes the sheath to stretch and start to pull off the end of the cable.

A load of 1,500 pounds per square inch produces measurable elongation of the sheath between grips almost as soon as the load is applied.

There is a very appreciable, if not destructively excessive, increase in the rate of elongation of the sheath at a stress of 2,000 pounds per square inch.

With the rate of loading used, the sheaths ruptured at apparent stresses of 2,300 to 4,300 pounds per square inch, calculated on the basis of the average area of rings taken from the ends of the cable sample after test.

The indicated tensile strength of lead sheath recorded in Table II shows higher average and maximum values for single-conductor cable than for multiple-conductor cable. This is likewise true of minimum values with but one exception. The tensile strength indicated for three-conductor cable sheath over light wall insulation is about the same for belted and type-*H* cable; however, one unusually low value was obtained on a sample of belted-type cable. Approximately a 15 per cent higher average tensile-strength value of sheath was obtained from three-conductor type-*H* cable with heavy wall insulation than for belted cable. The relatively high apparent stress values indicate that part of the load applied was transmitted to the insulation and conductor.

The apparent tensile strengths of lead sheath given in Table II are appreciably higher than should be expected from tensile tests on samples of commercially pure lead sheath as reported by Moore and Allerman in Engineering Experiment

Table VC. Summary of Application of Load and Test Results

C. Third Set

	Specimen			
	9a	9b	9c	9d
Time of loading (seconds)	90	180	300	450
Time of loading (minutes)	1½	3	5	7
Area of sheath (square inches)	1.327	1.319	1.289	1.306
Stress at failure (pounds per square inch)	3,310	3,110	3,080	3,390
Rate of stress application (pounds per square inch per minute)	2,210	1,035	615	485
Load at failure (pounds)	4,300	4,050	3,800	4,420
Rate of loading (pounds per minute)	2,870	1,350	760	605
Elongation 10 inches (inches per inch) measured back of the grip	0.090	0.073	0.072	0.062
Vacuum (inches of mercury)				
Top end	+2½	−½	+4	−7
Bottom end	−26⅜	−24	−27¾	−20¾
Outside diameter, inches				
Before test	3.03	3.05	3.05	3.05
After test	2.98	3.00	2.99	2.99
Elongation 10-inch gauge lengths (inches per inch)				
Top 1	0.027	0.035	0.035	0.037
2	0.043	0.048	0.046	0.032
3	0.080	0.067	0.077	0.035
4	0.148	0.135	0.108	0.067
5	0.304*	0.263*	0.233*	0.165
6	0.165	0.218	0.158	0.317*
7	0.035	0.055	0.032	0.049
8	0.000	0.010	0.000	0.007
Bottom 9	End seal	End seal	End seal	End seal
End displacement (inches)				
Before loading	1⅛	⅝	½	1½
After loading	10	9¾	8¼	8⅝
Failure occurred				
Within grip				
From back end	1 inch	3 inches	2 inches	9 inches
Circumferential twist				
Bench marks (feet)	5	5	5	5
Rotation (degrees)	17	17	None	None

* Nearest the point of failure.
Elongation for 9*d* at 6 large owing to double necking down.

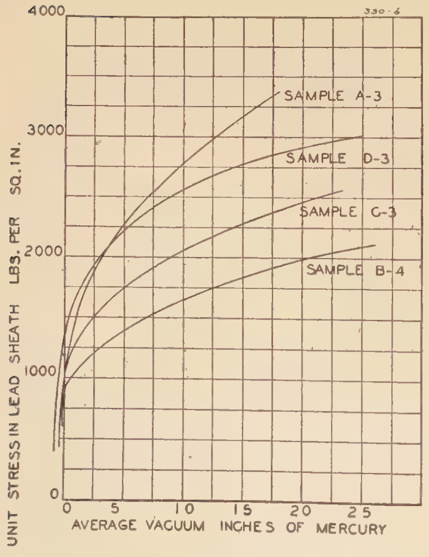

Figure 6. Stress-vacuum relation—lead cable sheath

Table VI

100 Per Cent Lead	Pounds Per Square Inch	
	Longitudinal	Transverse
G	2,010	2,200
H	1,590	1,610
I	1,530	1,570
J	1,910	1,965
Average	1,760	1,838
Deviation from average (per cent)—plus	14	20
minus	13	14

Table VIII. Lead Bend Tests

Test Number	Simplex Method (Cycles)	Standard Method 90-Degree Bends			Description at Bends
		Maximum	Minimum	Average	
0	$11\,^1/_2$–12	63	45	53	Knife edges
1	$10\,^1/_2$–12	79	39	53	
2	10 – 9	63	45	51	
3	11 –11	69	49	56	
4	7 –$11\,^1/_2$	73	43	54	
5	12 –10	57	43	48	
6	$9\,^1/_2$– 9	71	43	50	
7	10 –10	71	51	58	
8	9 –12	69	43	52	
Range		57–79	39–51	48–58	
Over-all range	7–12		39–79		

The test numbers indicate the number of feet from the top end of the test length to where the rings were cut. The sheath was pulled apart between six and seven feet from the end. Two tests were made by the Simplex method at each location.

Station, University of Illinois, *Bulletin* 243, "The Creep of Lead and Lead Alloys Used for Cable Sheathing,"[6] and Insulated Power Cable Engineers Association "Commercially Pure Lead Sheath for Power Cables," technical report 2, September 1936.[7]

An indication of the effect of loading upon thickness of sheath throughout the length of preliminary sample 1 and in the immediate proximity of the failure may be had from Table VII, discussed in Appendix III.

Determination of the uniformity in quality of commercially pure lead sheath from so representative an assortment of samples as source, diameter, and thickness of sheath seemed desirable. Two methods of bend test were used in order to find out whether results would be equally uniform as to the magnitude of values and variation range. The results of this investigation are reported in Appendix IV.

Special Investigation

It was apparent from the test results under the original investigation that answers to several important questions were lacking.

It was desired to know how the stretch throughout the length of the sample was distributed and to evaluate these incremental elongation values with that of the unfractured sheath recorded in Table III.

It was thought that a supplemental investigation of a series of samples having a common quality of commercial lead sheath and tested under different loading conditions would develop some of the answers.

Table VII

Test Number	Lead Thickness (Mils)		
	Maximum	Minimum	Average
0	140	123	131
1	122	107	113
2	128	108	117
3	124	108	113
4	125	113	118
5	125	112	117
6	114	102	110
7	131	110	120
8	147	119	130
Range	114–147	102–123	110–131
Over-all range		102–147	

Sheath fractured between the sixth and seventh foot.

Special Tests

The results of these special tests are reported here as a supplement to the original report.

It was thought that a longitudinal reinforcement of friction tape with a helical wrapping of like material over the sheath which extended from ten inches inside to two inches beyond the back end of the grip would shift the sheath fracture to occur between the regular bench marks. This it failed to do, but it did eliminate any appreciable tendency of the grips to slip. All special test samples were so reinforced.

The three sets of special test results are reported individually because of variations in procedure. (Tables V*A*, V*B*, and V*C*.)

A. First Set. The purpose was to determine the extent of the elongation that occurred in various sections of the sample lengths and to compare the strain on the sheath between bench marks at the center of the sample with that in the section under the grip which includes the failure.

The sheaths of two specimens were graduated by 10-inch and 20-inch bench marks. Wire pulling grips were used on both ends of the samples. The load was applied gradually, and readings were taken at each increment increase of 1,500 pounds, allowing only time necessary to take manometer reading and to measure the elongation and diameter over the sheath between the bench marks at the center of the specimen.

Samples used were three-conductor 500,000-circular mil Compack $^{22}/_{64}$-inch paper, $^{9}/_{64}$-inch lead.

B. Second Set. The purpose was to determine the time-ultimate stress relation, the variation in elongations between ten-inch bench marks, throughout the length of the samples, and the variation in strain at the center of the sample and in the location containing the failure. Wire grips were used on both ends of the samples.

The ultimate stress was estimated as 3,000 pounds per square inch for all rates of loading. The time intervals of loading were set to approximately $1\,^1/_2$, $2\,^1/_2$, 5, and 7 minutes. These rates of loading were reasonably well realized by the use of a stop watch and the testing-machine loading valve.

The same cable was used as that for "*A.* First Set."

C. Third Set. The first and second sets of special tests show that stretching of the sheath takes place more or less

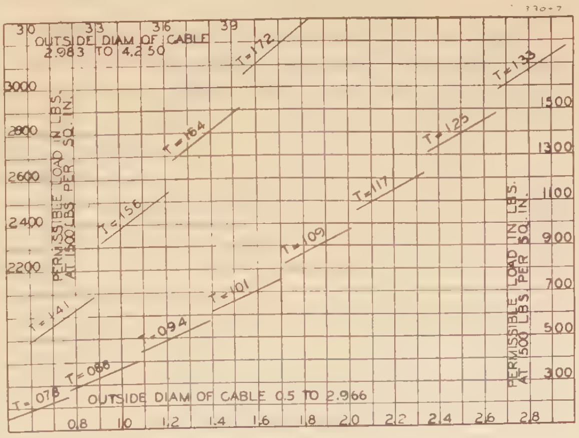

Figure 7. Permissible load in pounds on lead-encased cable

evenly at both ends of the samples when pulled with woven-wire grips.

The purpose of this set of tests is to determine how the elongation of the sheath takes place when the relative effective length of the stretched sheath is doubled by placing a pulling eye on one end and a woven-wire grip on the other end of the sample.

An ultimate stress was estimated as 3,000 pounds per square inch for loading intervals of $1^1/_3$, $2^1/_2$, 5, and 7 minutes respectively.

The sheath was graduated by ten-inch bench marks. The increment laid off just outside of the grip was used for elongation measurement readings while the sample was being loaded. The outside diameter recorded was taken halfway between these bench marks. The lower of these bench marks on the grip end was located about four inches above the back end of the grip on account of the reinforcing tape over the sheath.

A line was drawn from end to end of each sample to be used as a reference in determining whether or not the cable twisted while being pulled.

The samples were mounted in the machine with the wire pulling grips at the bottom.

The end displacement is an average of several probe depth measurements through the valve stem in the end seal, minus the length of the valve stem.

Samples used were three-conductor 500,000-circular mil Compack $^{19}/_{64}$-inch paper, $^9/_{64}$-inch lead.

Elongation of the sheath extends back to the wipe at the pulling-eye end. It is expected that elongation of the sheath would take place over a greater distance if longer samples could have been used.

Conclusions

If stresses in commercially pure lead sheath are in excess of 1,500 pounds per square inch, vacuums of high magnitude are quite likely to develop.

Lead sheath can be stretched seriously without the damage being apparent from visual inspection. Serious stretching can be detected as variations in diameter tape measurements.

There is a possibility that in some instances the insulation quality might be reduced because of a change in the mechanical uniformity of the insulation underlying sheath that has been stretched excessively.

Excessive stretching of the sheath and the resulting disturbance of the mechanical uniformity of the underlying insulation could account for excessive cable heating and many manhole and joint failures of the past.

There is need for extreme caution, when luffing or ratcheting cable for slack in manholes, or in removing cable to be reinstalled, not to damage it by stretching the sheath excessively.

The extent of stretching occurring during pulling or as a result of adjustment of the cable in the duct by luffing can be determined readily by placing ten-inch bench marks along the sheath under and just beyond the back of the grip.

Excessive stretching of the lead sheath off the end of the cable produces a high degree of vacuum which may be responsible for the creation of voids within the insulation some distance back from the end of the cable through the transfer of compound from the insulation into the space developed at the pulling end of the section.

It is possible that the effect of this migration of compound may have accounted for an appreciable percentage of cable failures in the pulling end of section lengths within the manhole or adjacent to the conduit entrance.

There is appreciable cold flow of commercially pure lead sheath if a stress of 1,500 pounds per square inch is exceeded, which not only reduces its effective thickness but may reduce its flexure fatigue resistance.

Overstraining of the cable sheath might be avoided in many cases by checking experienced judgment against a precalculated value of maximum recommended load or cable length by means of the proposed formulas.

Calculation of the maximum permissible load to which a cable should be subjected is of questionable value for marginal cases, unless some practical device is available that will indicate continuously the actual load to which the cable is subjected or limit the load at the chosen maximum permissible value during installation. If such facilities are not available, there is no way of telling whether the cable sheath may have been overstressed during installation.

The use of woven-wire pulling grips can be justified with practically any size or type of cable where there is adequate duct room and the safe maximum stress on the sheath is not exceeded.

When deciding upon the type of connection to be used on the pulling end of the cable in marginal cases, consideration should be given to the value of the cable per foot that may be lost through damage from excessive stretching of the sheath and the potential saving in reducing manhole slack allowances if pulling eyes are used. In case of doubt, as with borderline cases, it is always safer to require the attachment of pulling eyes.

Factual field information gathered from installation experience through the use of the simple device of placing ten-inch bench marks along the pulling end of the cable would prove valuable in establishing some limit of elongation of sheath beyond which it could be considered quite definitely that the quality of the cable had been impaired.

Samples of cable can be prestressed by the use of pulling grips to a point where the sheath has been stretched excessively. These samples could be subjected to cyclic loading tests to determine what effect the disturbance of mechanical uniformity caused by excessive stretching of the sheath has on the performance in comparison to that of an adjacent nonprestressed sample of the same cable.

In the case of small conductors where the permissible load would be limited by the formula 0.008 × conductor circular mils, the permissible load on the lead sheath may be added to that permitted on the conductor if the conductor and lead sheath are joined securely to a pulling eye.

It is hoped that a study of the data presented will stimulate discussion of an old subject and lead to the development of more adequate control facilities for field use and to the adoption of a few practical rules for the guidance of men in the field, who, after all, carry the responsibility for the proper handling of cable installations.

If, as a result of this work, there is any contribution that will make it possible for cable engineers of the utilities and the manufacturers to discriminate as to when pulling eyes or socketing of conductors into the end seals should be specified in preference to the conventional type of end seal, the expense and effort will have been justified.

Appendix I. Excerpts From Literature

The National Electric Light Association's underground systems committee reports on cable installation records current practice among utilities as of 1925.[1] The 1926 report[2] summarizes opinion as to the effect of stress upon the cable and states the means of judging damage as follows:

> "Information obtained—principally from the cable manufacturers—leads to the conclusion that the electrical characteristics will not be effected if the cable physical characteristics be not disturbed.
>
> "In summarizing the reports from the manufacturers and from the operating companies, it would appear that in the ordinary installations of cable no serious harm can be done the cable unless there be visible mechanical injury, which has led one of our members to inject the thought that it is 'highly undesirable to install such long lengths of cable as

> would overstress the lead sheath either in installation or removal, having in mind the fact that in many cases it is necessary to take extra hitches on the cable in order to secure sufficient slack in the manhole into which the cable is pulled."

There is available extensive information relative to stresses on cables fitted with pulling eyes as a result of the field experiments conducted by Carroll H. Shaw.[3]

Factors influencing the coefficient of friction found in connection with cable pulls are presented in the NELA underground systems report on "Cable Installation for 1930."[4] The committee's conclusion as to available material relative to coefficient of friction range to be considered in the pulling of cable is quoted from NELA underground systems Reference Book.[5]

> "A conservative rule of thumb that has been used allows a pulling tension of 0.75 times the total weight of cable being pulled. If this limit is greatly exceeded, it would appear advisable to look for possible damage to the cable sheath such as scoring, gouging, or deformation. Certainly under no conditions should the stress be large enough to produce permanent elongation of the copper. The factor 0.75 mentioned above is the coefficient of friction between cable and duct. This varies widely depending upon conditions.
>
> "For well-lubricated well-constructed straight ducts with ample clearance between cable and duct the coefficient of friction will average about 0.40."

The NELA underground systems committee, in its 1930 report on cable installation,[4] summarized its conclusions on this phase of their report as follows:

> "The problem has been considered by the subcommittee this year on the basis that:
>
> "1. No limit to the pulling stress has been set by the manufacturers or users of power cable, and it is therefore one of the duties of the committee to render a decision on this point.
>
> "2. The maximum stress to which the cable is subjected is the only figure of value in determining whether
>
> "(*a*). The cable has been injured during installation.
>
> "(*b*). A value for the safe maximum stress to which a cable may be subjected.
>
> "In discussion of the possibility of setting a safe value for the maximum pulling stress, it will be found in all cases that the injury to the cable is evident on the sheath of the leading end, except in those cases where, through oversight, the cable has jammed at the entrance to the duct. In the latter circumstance the dynamometer reading will not indicate necessarily that damage has been done. If, however, the cable has been overstressed mechanically, the leading end is the only portion of the cable which has sustained the maximum stress, and this is the stress recorded by the dynamometer. The stresses which exist in the cable behind the leading end are unknown and indeterminate by any practical measuring device but of a lower magnitude than the stress on the leading end which has already been determined. The leading end, however, is brought out for visual inspection at the end of the pull, and, if the conductors are not pulled away from the sheath, and the lead is not damaged, there is no possibility that dynamometer readings could indicate other damage.
>
> "This accessory is important, however, for improving installation methods and devices, especially in connection with new or unusual cable types, and a great deal of experimental work has been done by the larger companies along this line."

Appendix II. Short-Time Tensile Strength of Lead Sheath

The apparent stress values obtained from the samples of cable tested and shown in Table II are appreciably higher than reported tensile-strength values for commercial lead cable sheath.

Short-time tensile test results reported by Moore and Alleman[6] on sections taken from samples of cable sheath in both longitudinal and transverse directions to the axis of the sheath and tested at a head speed of 0.02 inch per minute are given in Table VI.

Results of short-time tests on circumferential strips of lead sheath reported in connection with the joint Association of Edison Illuminating Companies and Insulated Power Cable Engineers Association research technical report[7] show for commercial cable sheath without reference to quality of lead:

Weighted average of all	average value	2,362 psi
	minimum value	1,569 psi
	maximum value	2,660 psi

These results cover tests made at 11 different laboratories on samples taken from the same sheath lengths. Testing speeds varied from 0.2 to 1.25 inches per minute.

Appendix III. Physical Test Results from Lead Sheath After Being Subjected to a Loading Test

It was thought desirable to get some indication of the effect of stretching on thickness and physical characteristics of the sheath throughout the length of a sample, as indicated by the IPCEA and the Simplex equipments and procedures for bend tests. (See Tables VII and VIII.)

One of the samples used for preliminary tests in setting up the test procedure was ringed at the ends and at one-foot intervals. One two-inch ring and four one-half-inch rings taken at each location gave the results shown in Table VII.

Figure 8 shows the location and numbering of test rings in the immediate proximity of the failure of preliminary sample 1.

A one-half-inch ring was taken for standard bend test as close as possible to the sheath fracture (1*B*), one above (1*T*), and the other below (1*B*). Adjacent to the lower ring 1*B*, successive rings 2*B*, 3*B*, and 4*B* were examined with the results indicated in Table IX.

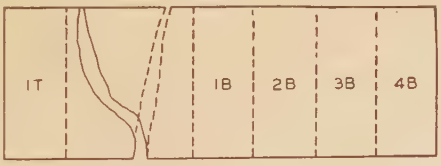

Figure 8. Diagram showing the location and numbering of test rings adjacent to failure

T—Top end during pulling strain test
B—Bottom end during pulling strain test

Table IX

Sample	Thickness—Mils Maximum	Thickness—Mils Minimum	Thickness—Mils Average	IPCEA Bends 90 Degrees Maximum	IPCEA Bends 90 Degrees Minimum	IPCEA Bends 90 Degrees Average	Cycles (Simplex) 1	Cycles (Simplex) 2
1*T*	106	95	102	57	47	50*		
1*B*	121	86	102	55	45	51*		
2*B*	120	96	108					
3*B*	113	102	107				7 1/2	7 1/2
4*B*	114	104	107	59	45	51*		

* All knife edges.

The fractures follow the helices of the paper tape directly under the sheath. The lead thins down in a long taper to a knife edge at the fracture as expected. Stretching of the sheath apparently did not change the bending-test results seriously.

Appendix IV. Bending Tests on Samples of Lead Sheath

The standard bend-test procedure and equipment required under the Specification for Impregnated Paper Insulated Cable, Solid Type (sixth edition, November 1937), high tension cable committee, Association of Edison Illuminating Companies, and an alternate known as the Simplex method were used. The latter has been under consideration as a possible standard for procedure in manufacturing control work. It is made quickly and involves the entire circumference of the sheath in a single test.

The rings for test were taken about four inches back of the seals from each end of the test length. Adjacent rings from each end were used for the respective tests. The sheath had not been abraded because of the protection by the pulling grip reinforcement.

Table XA

Cycles	Set 1	Set 2
Average	11.7	11.4
Minimum	9	7
Maximum	14.5	14

Table XB

	All Top Samples	All Bottom Samples
Average	11.5	11.7
Average—all samples	11.6	
Maximum spread—all samples	7 to 14.5	

The arithmetical averages of the number of 90-degree bends and the range of values under the standard method are as follows:

Maximum 69.3 with spread 57–83
Minimum 47.1 with spread 31–61
Average 57.7 with spread 48–67
Over-all range 31 to 83 bends

The arithmetical average of the performance tests made on the Simplex tester showed for two sets of samples, each set consisting of a ring from the top end and from the bottom end of the cable length, the results shown in Table X*A*.

If tests of all top samples and tests of all bottom samples are averaged, they show the results given in Table X*B*.

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Ampere Load Limits for Copper in Overhead Lines

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Synopsis: The paper presents a method of determining approximately the maximum continuous current-carrying capacity of conductors in overhead lines, as fixed by certain operating limits of temperature and time, beyond which it is expected that the physical characteristics of copper conductors might be materially impaired. The paper deals illustratively with conditions in the Philadelphia area, but the method is equally adaptable to any situation.

The design limits of ampere ratings determined herein, are from 10 to 35 per cent higher for bare conductors, and from 10 to 20 per cent higher for covered conductors, than those previously used in the Philadelphia area. Operation at load equal to the design limit will, during 99.93 per cent of the hours in the average year, maintain copper temperatures below 100 degrees centigrade in normal operations and below 135 degrees centigrade in emergency operations.

UNDER ordinary conditions and particularly in the design of new single-purpose lines, there is little doubt that some sort of cost balance akin to Kelvin's law favors conductor sizes sufficiently large to give operating-temperature limitations a place of rather remote interest. Considerations of voltage variation and the advance provision for expected load growth in general-purpose distribution lines also favor the initial installation of conductor sizes considerably larger than the minimum that could be used if necessary.

The present emergency naturally compels a reconsideration of the design limits of conductor size, but not without a possibility of substantial future advantage to those central-station systems which will utilize the information thus gained to make the maximum use of an installed line-conductor size, rather than to abandon it in favor of an avoidable change to a larger conductor size. In the authors' experience there has been no instance where the natural cost balance that normally favors oversize conductors in new lines has been sufficient to offset the large expense that must be accepted when an otherwise adequate line conductor size must be abandoned in order to provide a larger size.

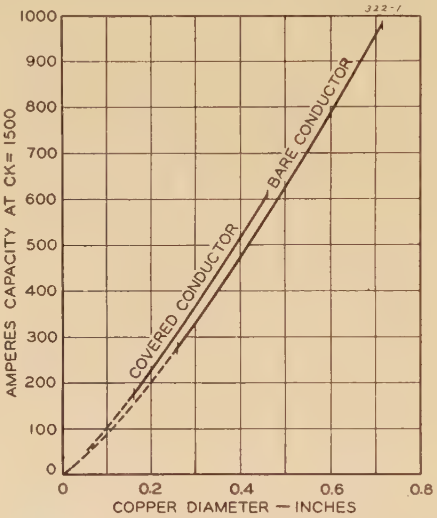

Figure 1. Principal effect of conductor diameter on ampere capacity of overhead lines

$$I = CKd\sqrt[4]{d+2s}$$

Line accessories such as conductor clamps and disconnects can have limitations that will become critical. Likewise, it is always possible that conductor clearances will need to be reconsidered at critical points, along lines which possibly might be required to operate at abnormally high conductor temperatures. Such limitations may be removed by thoroughly normal procedure and, usually, at much lower cost than to increase the conductor size. The working limits of the conductor material in its cooling medium must be known, however, before the other factors can be considered appropriately. This paper, therefore, centers its attention upon an approximate appraisal of present knowledge about the physical limitations of copper and a reconsideration of the natural heat-dissipating properties of conductors in overhead lines, as related to their effect upon the practical design limits of ampere loading for overhead lines.

Mechanical Limitations of Bare and Covered Copper

To the extent that copper may be said to have elasticity, then elasticity is the property of overhead line conductors which is most affected by temperature and time. Soft-drawn copper has very little and hard-drawn copper has the most but whatever elasticity is possessed by any conductor will be impaired to the extent that temperature and time may anneal the copper. The natural drawing effect which accompanies the mechanical stretching of copper imparts some measure of elasticity to it which it did not possess previously. Hence, mechanical tests of copper conductors, after partial annealing without mechanical stress may not be directly comparable to similar tests of identical conductors similarly heated, while kept under normal stringing tension. All of these factors contribute to a natural uncertainty that makes the mechanical characteristics of copper at comparatively low temperatures appear to be almost akin to the mechanical characteristics of alloy steels at much higher temperatures.

There appears to be considerable uncertainty, for instance, whether copper begins to anneal at 100 degrees centigrade or at 130 degrees centigrade. The range of uncertainty suggests, however, a probability that operating temperatures may be permitted to exceed 100 degrees centigrade on occasion, without material effect upon the mechanical properties of overhead conductors, provided the temperature will seldom exceed, say, 130 degrees centigrade.

There are occasions, happily very infrequent in well-managed distribution-system operations, however, when the

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rare coincidence of heavy loads with the failure of supply plant places the operator in a dilemma. His alternatives in such a circumstance are to mistreat certain surviving supply plants, to cut off certain customers, or to delay restoration of interrupted service. His choice naturally favors the customer, up to the point where the imminence of surviving plant failure seriously threatens inconvenience to other customers as well. The customer is pretty reasonable about a service interruption, so long as he knows that the interruption was unavoidable. If he were a metallurgist, however, he probably would be reluctant to agree that literal observance of a 130 degree centigrade line-conductor temperature limit was sufficient reason for a service interruption. He might have some reason to suspect that temperatures as high as 175 degrees centigrade to 200 degrees centigrade might be accepted for an hour or two without material impairment to the line conductor, because that is the range in which the annealing time is known to be in the order of days rather than weeks or years.

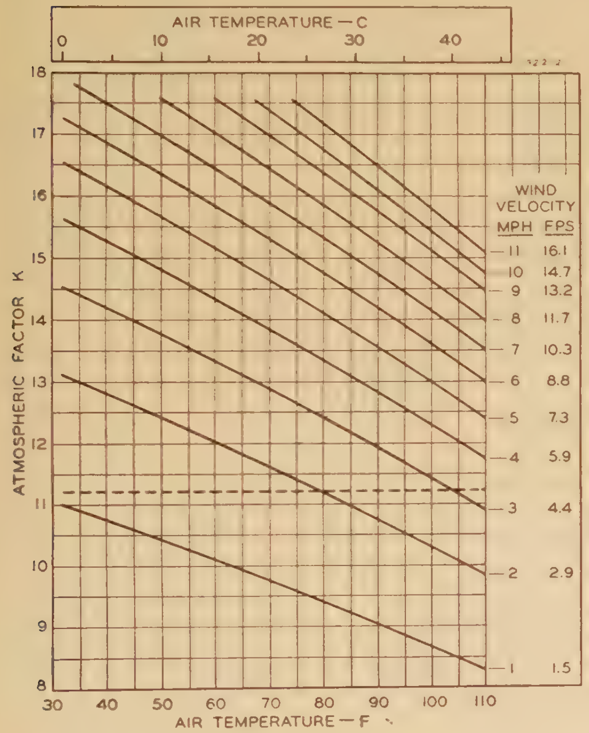

Figure 2 (left). Effect of air temperature and wind velocity upon atmospheric factor *K*

100 degrees centigrade surface—overhead

$K=\sqrt[4]{(t_s-A)^2v}$

where

t_s = temperature of conductor surface—100 degrees centigrade

A = ambient temperature of air—degrees centigrade

v = wind velocity—feet per second

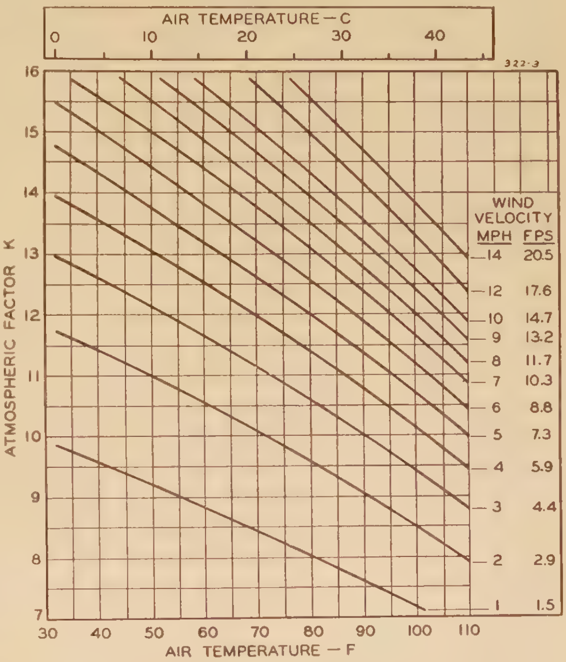

Figure 3 (right). Effect of air temperature and wind velocity upon atmospheric factor *K*

80 degrees centigrade surface—overhead

$K=\sqrt[4]{(t_s-A)^2v}$

where

t_s = temperature of conductor surface—80 degrees centigrade

A = ambient temperature of air—degrees centigrade

v = wind velocity—feet per second

A test was made, therefore, to determine approximately what damage the conductor and its automatic line splices might sustain during operation for a few hours at 175 degrees centigrade to 200 degrees centigrade. The test was made in a 12-foot frame, in which four short sections of bare medium hard-drawn, number 00 stranded copper conductors and three automatic line splices were connected in series and stressed to a constant tension of 1,000 pounds—the approximate stringing tension. Thermocouples were placed on two sections of the conductors and in each of the line splices. A temperature of 200 degrees centigrade was maintained essentially constant at a control point on one section of the conductor for about eight hours a day on six successive days for an aggregate of 44 hours. During the test, the other section of conductor maintained an essentially constant temperature of 180 degrees centigrade. The line splices were about 20 degrees cooler than the conductors and gave no evidence of any change in their contact resistance.

After the temperature tests were completed, approximate (eight-inch extensometer) stress-strain tests were made on each of the test samples and also on an unheated sample of the original conductor from which the test samples had been cut. The unheated sample of number 00 conductor broke at 5,500 pounds tension after about 0.9 per cent elongation. The 180 degrees centigrade sample broke at 4,600 pounds tension after 1.7 per cent elongation, whereas the 200 degrees centigrade sample had 30 per cent elongation without breaking at 3,600 pounds tension. In each test, the strength of the splices was superior to that of the conductor.

Thus, it was concluded that the physical characteristics of the copper conductor and its automatic line splices would not be altered appreciably by short-time operations up to 175 degrees centigrade on rare occasions, but that 200 degrees centigrade may cause serious annealing of the copper. The region between 175 degrees centigrade and 200 degrees centigrade appears to be a very critical one. Therefore, 175 degrees centigrade was accepted as the reasonable operating temperature ceiling for overhead copper conductors during the very rare emergencies in the life of a line when there might be a one- or two-hour coincidence of plant failure, maximum load, high ambient temperature, and low wind velocity.

The condition of the cover is a subordinate consideration. Hence the temperature of the cover, if present, should be correlated with the copper-temperature limits. Approximate calculations had indicated that 175 degrees centigrade copper would make about a 130 degrees centigrade maximum outside surface temperature for triple-braid weatherproof covering. This limit is severe but is still well below the 182 degrees centigrade (360 degrees Fahrenheit) minimum impregnating temperature recommended in the Purdue University *Engineering Bulletin* 43, November 1932. A drip test made on six standard samples of number 6 triple-braid weatherproof wire, oven-heated to 82 degrees centigrade for one hour, then to 130 degrees centigrade for two hours, developed bubbles on four samples after the first half-hour at 130 degrees centigrade. Two samples showed no ill effects, and no compound dripped from any of the samples so tested. Time is a factor, hence the over-all effect of temperature and time is expected to be slight during a few nonconsecutive hours in the life of the cover, when there might be a rare coincidence of critical atmospheric conditions and emergency load. With 100 degrees centigrade copper, the outside-cover-surface temperature is estimated to be 80 degrees centigrade, or just below the 82 degrees centigrade standard acceptance specification for triple-braid weatherproof wire. With the design limits of conductor temperature thus approximately determined, it becomes appropriate to review the effects of ampere loading upon temperature.

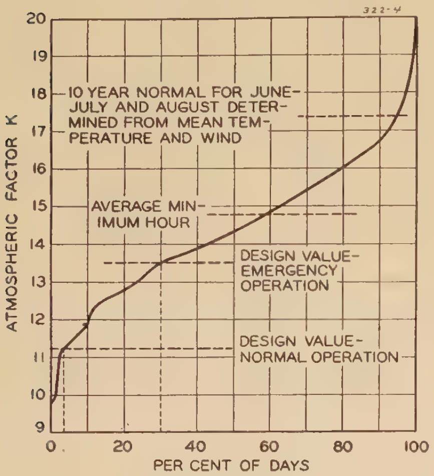

Figure 4. Minimum value of *K* in any hour for Philadelphia by days in June, July, and August, 1936–39

100 degrees centigrade surface

$K=\sqrt[4]{(t_s-A)^2V}$

where

t_s = temperature of conductor surface—100 degrees centigrade

A = ambient temperature of air—degrees centigrade

V = wind velocity—feet per second

Conductor Heating

The conductor heating effects are three and need little elaboration. At a given ampere loading, the power losses increase as the conductor resistance increases with temperature. Part of the power losses is dissipated by radiation according to the Stefan and Boltzman law. The remainder of the losses in outdoor conductors is dissipated by forced convection, for which an approximate formula has been developed by Schurig and Frick,[1] who have also shown that the effect of sunshine becomes very small in heavily loaded conductors. The natural heat balance is quite simple. The complete algebraic expression for the relation between amperes load and temperature is somewhat unwieldy because of the dissimilarity between the separate effects that must be correlated. By the method detailed in the appendix, however, it will be seen that the principal effects of conductor diameter, conductor-surface temperature rise above ambient, and wind velocity, may be factored out so as to give the following convenient expression for ampere carrying capacity:

$$I=CKd\sqrt[4]{d+2s}$$

where d is the conductor diameter, s is the cover thickness, K is the atmospheric factor, and C is a slow-moving residual coefficient which is essentially a constant for any given set of design limitations.

Table I

		Normal Rating	Emergency Limit
Bare-Stranded Conductors			
Circular mils	500,000	1,090	1,350
	450,000	1,030	1,270
	400,000	950	1,160
	350,000	880	1,070
	300,000	800	970
	250,000	710	850
Number	0000	640	760
	000	550	650
	00	470	560
	0	410	480
	1	360	410
	2	300	350
Bare-Solid Conductors			
Number	0000	600	720
	000	520	620
	00	450	530
	0	390	460
	1	330	390
	2	290	340
Covered Conductors			
Number	0000	570	710
	000	500	620
	00	440	530
	0	380	460
	1	330	400
	2	280	340
	4	220	250
	6	160	180

The principal effect of the conductor diameter and its cover, if present, may be seen in Figure 1, for the illustrative condition ($CK=1{,}500$). The position of the curve for covered wire above that for bare wire, is not particularly significant. In Philadelphia, for instance, the apparent advantage of covered wire in Figure 1, is offset by an 11 per cent lower design limit of CK.

The atmospheric factor ($K=\sqrt[4]{(t_s-A)^2v}$) may be evaluated directly from United States Weather Bureau data by the use of the chart in Figure 2 for bare conductors, or that of Figure 3 for covered ones, as the case may be. The results of such a survey may then be summarized, as in Figures 4 and 5. Maximum ambient temperature is not coincident with minimum wind velocity in the Philadelphia area. The 15-year maximum ambient of 40 degrees centigrade was accompanied by an 11-miles-per-hour wind. Other maximum ambients ranged from 35 degrees centigrade at five miles per hour to 39 degrees centigrade at 13 miles per-hour. The most critical coincidence, 23 degrees centigrade at one mile per hour, made the lowest values of K shown in Figures 4 and 5. The condition did not persist for more than one clock hour in any day and occurred on less than six days in the average year.

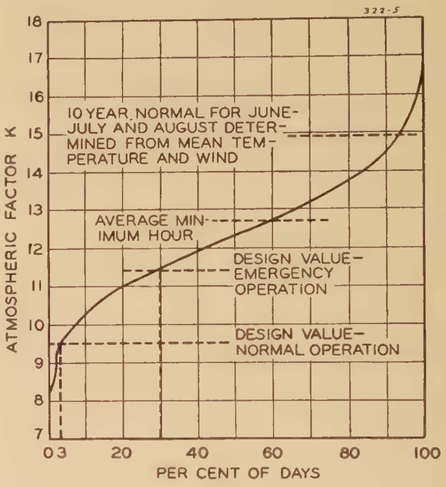

Figure 5. Minimum value of *K* in any hour for Philadelphia by days in June, July, and August, 1936–39

80 degrees centigrade surface

$K=\sqrt[4]{(t_s-A)^2V}$

where

t_s = temperature of conductor surface—80 degrees centigrade

A = ambient temperature of air—degrees centigrade

V = wind velocity—feet per second

It was considered reasonable therefore to permit copper temperatures to reach values in the neighborhood of 130 degrees centigrade with normal load or 175 degrees centigrade for any emergency load that might on a few rare occasions be coincident with so critical an atmospheric condition. The design values of K that are indicated in Figures 4 and 5 were selected so as to observe 130 degrees centigrade as the approximate ceiling for normal operations and 175 degrees centigrade as the ceiling for emergency operations. A summary of the resulting approximate maximum hour conductor temperatures during the three hottest months of the year is given in Figure 6, which was developed by using arbitrarily the value of C that would give the maximum estimate of copper temperature.

As should be expected, the residual factor C does not vary through nearly the range of values that is characteristic of K. For normal design-limiting conditions in the Philadelphia area, the value is within 4.5 per cent of $C=126$ for bare-stranded, within three per cent of $C=142$ for bare-solid, and within 5 per cent of $C=148$ for covered conductors. For emergency operations in Philadelphia the most critical values occur in the smallest conductor size and are respectively: $C=119$ for bare-stranded, $C=135$ for bare-solid, and $C=144$ for covered wire.

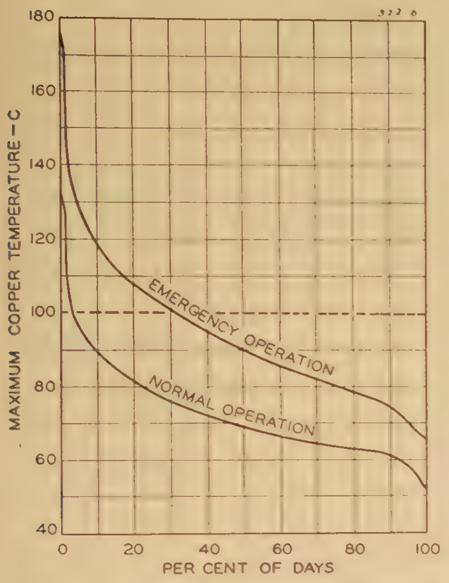

Figure 6. Approximate maximum-hour conductor temperature at ampere load limits in Philadelphia by days in June, July, and August

Ampere Design Limits in Philadelphia

Table I summarizes the ampere design limits for overhead copper lines in Philadelphia, as developed by the method described in this paper.

Winter load limits will be somewhat higher than given in the table. Tentative indications are that normal ratings may be increased ten per cent and emergency limits five per cent from November through March. The limiting atmospheric conditions are not much less severe than in the warmer months. However, about one half of the winter hours with critical ambient-temperature–wind combinations are accompanied by rain.

Conclusions

1. The paper deals with the carrying capacity of conductors in overhead lines, as determined by the effect of temperature and time upon the physical characteristics of the conductor. The paper does not deal with considerations of voltage variation, power loss, nor other factors that may favor larger conductors than are physically adequate for the immediate purpose.

2. The common assumption that maximum ambient temperature and minimum wind velocity are coincident is not supported by a study of United States Weather Bureau records for the past 15 years in Philadelphia.

3. The ampere load limits for overhead conductors in any situation can be evaluated directly from local records of critical ambient temperature and wind by the method given in this paper. The cooling effect of rain is not included, because it is absent during otherwise critical hours in summer and during one half of such hours in winter.

4. Coincidence of load-limit amperes with an hour of critical atmospheric conditions seems to be a contingency so remote that the resulting copper-temperature ceiling need be observed only as required in order to avoid weakening or oversagging the conductor.

5. Conductor temperature ceilings of about 130 degrees centigrade normal and 175 degrees centigrade emergency maximum appear permissible if of short duration and sufficiently infrequent. A 44-hour test has indicated that a few hours at 175 degrees centigrade should not change materially the physical characteristics of medium-hard-drawn copper wire or of automatic line splices, but that 200 degrees centigrade might cause serious annealing of the wire.

6. To recognize for the present at least the need for caution in utilizing temperatures approaching a critical zone for copper wires, the ampere load limits for Philadelphia are designed to keep copper temperatures regularly below 100 degrees centigrade normal or 135 degrees centigrade emergency. During the past 15 years, however, there has been an average of six nonconsecutive hours per year when a coincidence of load-limit amperes with critical atmospheric condition might make the copper temperature in some part of the line approach a ceiling of about 130 degrees centigrade normal or 175 degrees centigrade emergency maximum.

Appendix. General Expression for Current Ratings of Overhead Conductors

The current-carrying capacity of medium-hard-drawn copper conductors in outdoor overhead service was calculated for steady-state conditions. In the steady-state condition, the heat dissipation is equal to the I^2R loss in the conductor, or

$$I^2R=WS \text{ watts} \qquad (1)$$

where S is the surface area of the conductor, W is the sum of the watts radiated W_r and the watts dissipated by convection W_c, so that

$$W=W_r+W_c \text{ watts per square inch} \qquad (2)$$

In surroundings of temperature T_o degrees Kelvin, the heat dissipation by radiation from long horizontal cylinders, with surface of E relative emissivity and surface temperature of T_s degrees Kelvin, is

$$W_r=\frac{36.8E}{10^{12}}(T_s^4-T_o^4) \text{ watts per square inch} \qquad (3)$$

The approximate formula of Schurig and Frick[1] for heat dissipated at sea level by forced convection in air moving v feet per second crosswise to horizontal cylinder with outside diameters $(d+2s)$ ranging from 0.3 to 5.0 inches is

$$W_c=\frac{0.0128}{T_a^{0.123}}\sqrt{\frac{v}{d+2s}}(T_s-T_o) \text{ watts per square inch} \qquad (4)$$

where

$T_a=(T_s+T_o)/2$

d = copper diameter in inches

s = thickness of conductor cover in inches

Substituting equations 3 and 4 in equation 2

$$W=\frac{36.8E}{10^{12}}(T_s^4-T_o^4)+\frac{0.0128}{T_a^{0.123}}\sqrt{\frac{v}{d+2s}}\times(T_s-T_o) \qquad (5)$$

$$=(T_s-T_o)\left[\frac{36.8E}{10^{12}}(T_s+T_o)(T_s^2+T_o^2)+\frac{0.0128}{T_a^{0.123}}\sqrt{\frac{v}{d+2s}}\right] \qquad (6)$$

For convenience let

$$C_r=\frac{36.8E}{10^{12}}(T_s+T_o)(T_s^2+T_o^2)$$

and

$$C_c=\frac{0.0128}{T_a^{0.123}}$$

then

$$W=(T_s-T_o)\left[C_r+C_c\sqrt{\frac{v}{d+2s}}\right] \text{ watts per square inch} \qquad (7)$$

If it is assumed that about 75 per cent of the surface area of bare-stranded conductor is effective in heat dissipation, as is approximately true for radiation, then the effective surface area S of stranded copper is equal to that of a smooth cylindrical envelope and the heat dissipated per foot of conductor is

$$WS=12\pi(d+2s)(T_s-T_o)\times\left[C_r+C_c\sqrt{\frac{v}{d+2s}}\right] \text{ watts} \qquad (8)$$

The approximate average resistance per foot of medium-hard-drawn (97.5 per cent conductivity) copper of diameter d at T_c degrees Kelvin is

$$R=10^{-6}T_c/26.7(rd)^2, \text{ or 13.97 ohms per circular-mil foot at 100 degrees centigrade} \qquad (9)$$

where r equals 1.00 for solid conductors, or approximately 0.87 for stranded conductor. Thus, from equations 1, 8, and 9

$$I^2 = \frac{WS}{R} = \frac{320\pi(rd)^2(d+2s)\times 10^6}{T_c}(T_s-T_o)\times \left[C_r + C_c\sqrt{\frac{v}{d+2s}}\right] \qquad (10)$$

$$I^2 = \frac{320\pi r^2\times 10^6}{T_c}(T_s-T_o)d^2(d+2s)\times \sqrt{\frac{v}{d+2s}}\left[C_r\sqrt{\frac{d+2s}{v}}+C_c\right] \qquad (11)$$

$$I^2 = \frac{10^9 r^2}{T_c}\left[C_r\sqrt{\frac{d+2s}{v}}+C_c\right]\times \sqrt{(T_s-T_o)^2)v}\;[d^2\sqrt{d+2s}] \qquad (12)$$

or

$$I = CKd\sqrt[4]{d+2s} \text{ amperes} \qquad (13)$$

where

$$K = \sqrt[4]{(T_s-T_o)^2 v} = \sqrt[4]{(t_s-A)^2 v}$$

$$C = 31{,}600r\sqrt{\frac{1}{T_c}\left[C_r\sqrt{\frac{d+2s}{v}}+C_c\right]}$$

$$= 31{,}600r\sqrt{\frac{1}{T_c}\left[\frac{36.8E}{10^{12}}(T_s+T_o)(T_s^2+T_o^2)\times \sqrt{\frac{d+2s}{v}}+\frac{0.0128}{T_a^{0.123}}\right]}$$

A = ambient temperature in degrees centigrade
d = copper diameter in inches
E = relative emissivity of the surface
r = 1.00 for solid, or 0.87 for stranded conductor
s = cover thickness in inches
t_s = outside surface temperature in degrees centigrade
v = wind velocity in feet per second
T_o = ambient temperature in degrees Kelvin
$T_a = (T_o+T_s)/2$
T_c = copper temperature in degrees Kelvin
T_s = outside surface temperature in degrees Kelvin

Upon analyzing the factors in the expression for current, equation 13, it will be seen that:

K may be called an atmospheric factor and evaluated from Weather Bureau observations for any given design temperature t_s of conductor surface, as in Figure 2 for 100 degrees centigrade, or Figure 3 for 80 degrees centigrade.

C is a coefficient which compensates also for the residuals left after accounting for the major effects of d and K. C is practically a constant for all the usual overhead conductor sizes and combinations of high ambient temperatures with critically low wind velocities. For instance, all values of C lie within five per cent of 142 with the numbers 2 to 0000 sizes of oxidized (relative emissivity of 0.6) bare-solid conductors at temperatures from 100 to 175 degrees centigrade.

Reference

1. HEATING AND CARRYING CAPACITY OF BARE CONDUCTORS FOR OUTDOOR SERVICE, O. R. Schurig, C. W. Frick. *General Electric Review*, volume 33, 1930, pages 141–57.

Intrasystem Transmission Losses

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THIS paper describes a new method of calculating transmission losses within power systems. Reasonable accuracy is obtained with longhand calculation, although the same general scheme with the load-flow determinations carried out on a d-c calculating board would provide greater accuracy with less work. The use of an a-c calculating board with separate tabulations of kilowatt and kilovar flows would be even more advantageous. The method is based on the principle of superimposing the load distribution from each source, determining the current in each line as a sum of the individual load flows (with each in its proper direction), squaring this expression for current, and setting up an equation for losses in terms of megawatt generation at the various plants and of megawatt flows (in or out) at each interchange point. An outward flow of power at an interchange point is considered negative generation.

The procedure consists chiefly in deriving a special loss formula for a given power system. The formula is suitable for any condition of generation, load, and interchange. The formula involves only multiplication and addition, and it may, therefore, be used by semiskilled office personnel. Loss determinations for a large number of operating conditions may be made in an hour, thus making practicable the determination of losses for hour-by-hour readings on a dispatching log.

The loss formula may be used for determination of losses, either total or incremental, for billing, forecasting, or system-planning purposes. It has been used by several power companies for hour-by-hour determination of incremental losses. Other power companies have used the formula to derive a typical or average per cent loss to use over a month or longer period of time. The average per cent loss has been derived by calculating the losses for 10 to 50 or more representative hourly operating conditions and selecting an average value. Such determinations are reviewed monthly, or less often if system operating conditions are reasonably uniform from month to month.

Previous Methods

Heretofore it has been necessary to determine losses by detailed calculation of each individual line or by detailed measurement of each individual line on an a-c calculating board (network analyzer).

The determinination of losses by calculating each line separately is slow, even if applied to past operations where the actual load on each line has been measured. It is of little service for studies of future conditions in complicated networks where the load flows cannot be calculated easily. The longhand method of line-by-line calculation is too slow to use for hour-by-hour readings.

The a-c calculating-board method is of great value for system planning or for other purposes where only a few representative operating conditions are of interest. The time and cost of this method prevent its application to hour-by-hour readings.

Procedure for Determining Loss Formula

1. Secure estimated megawatt (three-phase) loads on primary substations for a heavy load hour (preferable annual maximum hour, when system losses are presumably the highest). Neglect transmission losses for the time being. On most systems the load distribution on a percentage basis changes very little from peak to off-peak, and the effect of power-factor variation is unimportant (except at off-peak on systems with high charging current). Loads at small stations may be prorated at terminal stations in inverse ratio to line impedance, without much effect on accuracy of the loss calculations.

2. Secure an impedance diagram of the high-voltage transmission system. Intermediate-voltage lines may be omitted without much effect on accuracy of the loss calculations.

3. Total the loads in item 1 and prorate in per cent (of 100 megawatts).

4. Determine Z for the $R+JX$ line values in item 2. (Disregarding power factor, the flow of current will divide inversely as Z in each circuit.) Convert Z to ohms at the major transmission voltage of the system. Draw a one-line diagram of the transmission system with the proper Z value (phase-to-neutral) marked on each line. (Use only the scalar value of Z, without regard to angle.)

5. Determine the location of generating sources and points of interconnection, existing or proposed, for the system. Very small plants may be netted with local loads before tabulating these loads in item 1. Any heavy load concentrated at one point and not following closely the system daily load curve may be considered as an interconnection point and handled as a power source (negative).

6. Draw up a hectograph or stencil one-line diagram with the lines in item 2 (omitting impedance values), with the substation loads in item 3, and with the sources in item 5. Consider each interconnection as a generating source (positive or negative). Since these one-line diagrams are to be used as work sheets in making up flow diagrams on a cut-and-try basis, 25 or more copies may be needed (unless a calculating board is available for the operation in item 7).

7. Make up a synthetic megawatt load-flow diagram for each separate source on a separate sheet, using the forms prepared in item 6. Consider each interconnection as feeding into the system like a generator, regardless of the normal flow of power at the interconnection point. Observe the direction of flow carefully and consistently as between diagrams, so that the algebraic sum of the megawatt flows in any line will be the correct one. Unless a d-c or a-c calculating board is available, the synthetic load-flow diagrams will have to be developed by a cut-and-try method, making the total of the products of impedance times proper flow around any loop equal to zero. Omit consideration of losses in making these flow diagrams, unless a calculating board is used. In any case the total output from any source should be 100 megawatts at the source. Omit consideration of load and generator power factor for the time being, unless an a-c calculating board is used, on the assumption that the proper load flow is secured when the sum of the *zmw* products around every loop in the system is equal to zero or approximately so. (Neglect fractions of megawatts.)

8. Set up a tabulation listing in the first column each line by the names of the two terminals, followed in the second column by the resistance of the line *in ohms at the major transmission voltage.*

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Table I

Sources	Square Terms	Product Terms	Total Terms
1	1	0	1
2	2	1	3
3	3	3	6
4	4	6	10
5	5	10	15
6	6	15	21
7	7	21	28
8	8	28	36
9	9	36	45
10	10	45	55

Power flowing from the first named terminal to the second should be considered as positive. It is convenient to choose the order of terminals so that most of the flows in each line will be positive. Horizontally opposite each line in separate columns set down the megawatt flows from item 7 for each source. In this discussion the various megawatt generator and interconnection loadings will be designated as G_A, G_B, G_C, and so forth.

9. Determine KV and *PF*, defined as follows: Let KV be the average phase-to-phase voltage (in kilovolts) for the heavily loaded portions of the transmission system (usually near generating plants). Let *PF* be the average decimal power factor in the heavily loaded portions of the transmission system.

10. Calculate *H*, which is a common constant factor to be applied to the square and product-flow totals to get three-phase kilowatt loss (see Appendix *A* for derivation).

$$H=\frac{(0.1)}{(PF\times kv)^2}$$

11. Set up a tabulation of the square terms listing the lines as in item 8 and horizontally opposite the product of resistance times the square of power flow in megawatts (with each source in a separate vertical column).

12. Set up a tabulation of product terms listing the lines as in item 8 and horizontally opposite the product of resistance times power flow from one plant times the flow from another plant. List separately each pair of sources, until each source has been used once with every other source.

13. Total the columns in item 11 vertically, and multiply each of the totals by *H*, thus forming the square coefficients in the loss equation. These coefficients will always be positive. In this discussion, these coefficients will be designated as K_{AA}, K_{BB}, K_{CC}, and so forth. Each coefficient when multiplied by the square of the symbol denoting megawatt generation at its respective plant becomes a term in the final complete loss equation (see Appendix *A* for derivation).

14. Total the columns in item 12 vertically, and multiply each of the totals by 2 *H*, thus forming the product coefficients in the loss equation. These coefficients may be either positive or negative. In this discussion these coefficients will be designated as K_{AB}, K_{AC}, K_{BC}, and so forth. Each coefficient when multiplied by the two symbols denoting megawatt generation at its respective two plants becomes a term in the final complete loss equation.

15. Set up the results of items 13 and 14 as follows:

Loss (in three-phase kilowatts) =

$$G_A^2K_{AA}+G_B^2K_{BB}+G_C^2K_{CC}+\text{ and so forth }+G_AG_BK_{AB}+G_AG_CK_{AC}+G_BG_CK_{BC}+\text{ and so forth}$$

Use of Loss Formula

16. The loss equation in item 15 consists of *K* values determined once for all and of plant and interconnection loadings designated as G_A, G_B, and so forth, selected for each case for which losses are desired. Use loadings in megawatts. The total loss will be in kilowatts. The algebraic sum of the loadings will equal the net system input. Net system input equals territorial load plus losses. Generation and tie-line receipts are considered positive. Tie-line deliveries are considered negative. The loss equation is now ready for use, unless it is desired to set up the results graphically, or to determine minimum loss conditions, or to derive a shorter approximate equation with only the major sources. After the coefficients

Table II. Conversion of Load Flows Into Loss Coefficients

Line Terminals	Resistance in Ohms at 60 Kv	Burton B	Washington W	Madison M	Nelson N	Anderson A	B^2 × Resistance	W^2 × Resistance	M^2 × Resistance	N^2 × Resistance	A^2 × Resistance
							+	+	+	+	+
A-B	18.1	13	12	−3	−3	6	3,060	2,610	160	160	650
B-C	38.2	6	5	−10	−10	−1	1,380	960	3,820	3,820	40
A-C	9.0	28	25	−26	−26	5	7,050	5,630	6,080	6,080	220
C-D	3.8	30	30	30	−70	30	3,420	3,420	3,420	18,620	3,420
C-E	6.0	−5	−9	25	25	−35	150	490	3,750	3,750	7,350
E-F	8.6	−9	−12	12	12	8	700	1,240	1,240	1,240	550
F-G	12.0	−15	−18	6	6	2	2,700	3,880	430	430	50
E-H	22.7	−2	−3	7	7	−49	90	200	1,110	1,110	54,500
H-G	30.8	−8	−9	1	1	45	1,970	2,500	30	30	62,370
G-A 60 kv	1.9	−21	30	−13	−13	25	840	1,710	320	320	1,180
G-A 12 kv	17.1	−25	20	−3	−3	−1	10,670	6,840	150	150	20
Total							32,030	29,480	20,510	35,710	130,350
Total ÷ 10^4							3.203	2.948	2.051	3.571	13.035
Above ×0.343							+1.10	+1.01	+0.704	+1.22	+4.47

		B×W × Resistance	B×M × Resistance	B×N × Resistance	B×A × Resistance	W×M × Resistance	W×N × Resistance	W×A × Resistance	M×N × Resistance	M×A × Resistance	N×A × Resistance
A-B	18.1	2,820	−710	−710	−1,410	−650	−650	1,300	160	−330	−330
B-C	38.2	1,150	−2,290	−2,290	−230	−1,910	−1,910	−190	3,820	380	40
A-C	9.0	6,300	−6,540	−6,540	1,260	−5,850	−5,850	1,120	6,080	−1,170	−1,170
C-D	3.8	3,420	3,420	−7,980	3,420	3,420	−7,980	3,420	−7,980	3,420	−7,980
C-E	6.0	270	−750	−750	1,050	−1,350	−1,350	1,890	3,750	−5,250	−5,250
E-F	8.6	930	−930	−930	−620	−1,240	−1,240	−820	1,240	820	820
F-G	12.0	3,240	−1,080	−1,080	−360	−1,300	−1,300	−430	430	140	140
E-H	22.7	140	−320	−320	2,220	−480	−480	3,340	1,110	−7,790	−7,790
H-G	30.8	2,220	−250	−250	−11,030	−280	−280	−12,470	30	1,390	1,390
G-A 60 kv	1.9	−1,200	520	520	−1,000	−740	−740	1,420	320	−620	−620
G-A 12 kv	17.1	−8,560	1,280	1,280	430	−1,030	−1,030	−340	150	50	50
Total		10,730	−7,650	−19,050	−3,450	−11,410	−22,810	−1,760	9,110	−8,960	−20,700
Total ÷ 10^4		1,073	−7,765	−1.905	−0.345	−1.141	−2.281	−0.176	0.911	−0.896	−2.07
Above ×0.686		+0.734	−0.524	−1.31	−0.236	−0.783	−1.56	−0.121	+0.625	−0.615	−1.42

are determined, the loss equation can be solved for any given loading in a few minutes, even if five or six sources are involved. Certain simplifications are possible in many cases, and the number of sources may be reduced without too great an effect on accuracy of the loss calculations. However, it is preferable to start with a comprehensive and accurate loss equation, so that the effect and validity of approximations may be determined.

Methods of Simplifying Loss Formula

17. In most cases two or more sources (plants and/or tie lines) in a small area may be totalized as one. One scheme of pooling is to determine from the complete loss equation new coefficients for the pooled sources which will give the same total loss as the individual coefficients for the distribution at the annual maximum hour. Errors at smaller system loads will be of little importance.

18. If any plant is operated at a flat base load all the time (or whenever losses need to be calculated), a numerical value of megawatts can be substituted for the respective plant loading symbol, thus eliminating one variable and simplifying the loss formula for most purposes. Sometimes a tie line may be handled similarly.

19. If any plant is either shut down or operated at full load, the above substitution and one for zero load can be made, resulting in two loss equations with one less variable. One or the other may be used, depending on whether the plant in question is on or off. Sometimes a tie line may be handled similarly.

20. The most effective and generally useful simplification of a loss formula is to make use of the fact that all power systems have a planned order of loading plants, except for infrequent emergency situations. Sometimes this order changes seasonally or with hydro or fuel conditions. Even if three different loading schedules are used during the year, three loss formulas with variables for only the regulated plants are preferable to one formula with a variable for each plant.

21. If losses must be determined frequently, tables are much superior to charts.

22. Even though the loss equation is used only to derive loss tables or monthly average loss percentages, any type of loss chart conveys so much information that it seems desirable to construct a loss chart, even if it is based on an approximate loss equation obtained by reducing the system to two or three major source areas.

23. Total loss varies as the square of the total system load for any given fixed ratio of loading between plants. Therefore, it is possible to take loss values from general charts plotted in percentage or decimal distribution and to secure total loss by multiplying the chart value by the square of the system load. The most convenient form of chart is one based on 100 megawatts total load, with the plant and tie loadings in megawatts (or per cent). The total system loss is then equal to the chart value multiplied by the square of the system load in hundreds of megawatts.

24. In those few cases where there are only two sources, or where the system can be approximated by only two major source areas, a rectangular co-ordinate chart of system loss can be drawn up. The co-ordinates may be "total net system input" and "source A"; or, "source A" and "source B." The loci of constant loss will be concentric ellipses in either case.

25. In systems involving three source areas with one source always base loaded, the loss equation contains only two variables and the above type of rectangular chart can be used. This chart has the further advantage that it is direct reading (instead of being on a 100-megawatt basis, as described in item 27).

26. In all cases where the loading of three plants or ties is of major interest, the use of triangular co-ordinate paper is the most feasible means of plotting loss curves.

27. In using triangular co-ordinate paper, the three plant loadings, in per cent (totaling 100) or in megawatts for 100 megawatts system load, are measured perpendicularly to each side of an equilateral triangle. (K and E triangular co-ordinate paper 359-32 or 344-A is convenient for this purpose.) The sum of any three such altitudes always equals the height of the triangle or 100 per cent. The same relation holds for points outside the basic triangle, if altitudes wholly outside the triangle are considered negative. This permits the plotting of deliveries to tie lines as negative generation.

28. For four or more sources with variable loading, no means of graphic representation seems to be possible. Nomographic charts cannot be used with this type of equations

Procedure for Determining Loading for Minimum Loss

29. While plant capabilities and relative power costs usually prevent allocation of plant and tie loading on a minimum transmission-loss basis, the determination of this optimum condition is frequently of value. Inspection of minimum loss loadings conveys a great deal of information on the adequacy of transmission planning in the past and offers a needed guide to adequate transmission planning in the future. It provides a means by which the load dispatcher can compare the delivered costs of power from sources of equal or nearly equal cost (at the source). It provides a starting point for empirical charts or formulas of transmission losses in those cases where the general loss formula after all possible rational simplification still contains too many terms for ready use.

30. The general equation for loss cannot be solved for *minimum* loss loadings until use is made of the fact that the sum of the loadings is equal to unity, if each is on a percentage basis. (Otherwise the partial-derivative equations are homogeneous, giving a determinant equal to zero.)

31. Take the partial derivative of equations in item 15 with respect to each one of the sources in that equation, giving as many partial-derivative equations as there are sources in item 15. Equate each derivative equation to $2x$.

32. *Assumptions as to system operation must be inserted into the equations in item 15 before taking partial derivatives in item 31.*

33. Rearrange the terms in the equations in item 32 with like sources in the same columns and with the columns and rows in the same order. Place the $2x$ terms on the right-hand side of the equality sign.

34. Subtract from the first partial-derivative equation in item 34 each of the other partial-derivative equations in turn. This will reduce the right-hand side of each new equation to zero, by eliminating the $2x$ term.

35. Divide each of the new equations in item 35 by the unknown in the first term. This makes the first term numerical, changes the unknowns to ratios, and reduces the number of unknowns to equality with the number of equations. The equations can now be solved simultaneously for the unknown ratios (see pages 35–7 of "Determinants" by Weld).

36. Solve the simultaneous equations in item 37, preferably by means of determinants. The results will be the loading ratios for minimum loss with the loading of each source variable.

37. If the simultaneous equations in item 36 are more than three in number, and if they are solved by addition and subtraction instead of by determinants,

care should be taken to eliminate between the first equation and each of the others in systematic order, so that each of the derived equations will be independent of the others.

38. Total the ratios in item 36, add unity for the ratio of the source in the denominator of each unknown, and prorate so that the new total of all source loadings equals unity. These decimal fractions totaling unity constitute the simultaneous loadings for minimum total loss, with the loading of each source variable.

39. While the preceding use of partial first derivatives is in general a necessary but not a sufficient condition of a minimum, it can be shown that the loss loci are concentric ellipses in any component plane and have a minimum at the center. Therefore, there can be no ambiguity about the point of minimum loss, and there is no need of taking second derivatives to determine sufficiency (see pages 484–6 of "Calculus" by Granville, Smith, and Longley).

Procedure for Constructing Rectangular Loss Charts

40. If only one source loading is variable, the loss equation can be represented by a parabola.

41. If only two variable sources are involved, a rectangular type of loss chart may be constructed from the loss equation derived in item 15. The general form of such an equation is as follows:

Kw loss $=K_{AA}A^2+K_{BB}B^2+K_{AB}AB$

42. If the equation in two variables has been derived by base loading one or more sources, there will be three additional terms as shown below:

Kw loss $=L_{AA}A^2+L_{BB}B^2+L_{AB}AB+L_AA+L_BB+L_C$

43. Either form of equation 41 or 42 may be represented graphically by an ellipse. (See page 66 of "Handbook of Engineering Fundamentals" by Eshbach. This reference shows that the loss equation in item 15 is a conic section in the plane of any two variables and that this section meets the test for an ellipse.)

44. The construction of ellipses representing loci of constant loss requires a systematic procedure if speed and accuracy are to be realized.

Table III. Loss Formulas

I. Transmission Losses—Using Two Source Areas—Overton-Madison Line and Anderson-Wilson Line Not in Service

S = megawatts generated at Washington plus Burton
N = megawatts generated at Nelson plus megawatts supplied by Riverton line at Nelson (negative if feeding out)
Kilowatt loss $=+0.71S^2+1.22N^2-1.43SN$

II. Transmission losses—Using Three Source Areas—Overton-Madison Line Not in Service

S = megawatts generated at Washington plus Burton
N = megawatts generated at Nelson plus megawatts supplied by Riverton line at Nelson (negative if feeding out)
M = megawatts supplied by Overton line at Madison (negative if feeding out)
Kilowatt loss $=+0.71S^2+1.22N^2+0.70M^2-1.43SN-0.65SM+0.62NM$

III. Transmission Losses—Using Four Source Areas

S = megawatts generated at Washington plus Burton
N = megawatts generated at Nelson plus megawatts supplied by Riverton line at Nelson (negative if feeding out)
M = megawatts supplied by Overton line at Madison (negative if feeding out)
A = megawatts supplied by Wilson line at Anderson (negative if feeding out)
Kilowatt loss $=+0.71S^2+1.22N^2+0.70M^2+4.47A^2-1.43SN-0.65SM-0.185A+0.62NM-1.42NA-0.62MA$

IV. Transmission Losses—Using Five Source Areas

B = megawatts generated at Burton
W = megawatts generated at Washington
N = megawatts generated at Nelson plus megawatts supplied by Riverton line at Nelson (negative if feeding out)
M = megawatts supplied by Overton line at Madison (negative if feeding out)
A = megawatts supplied by Wilson line at Anderson (negative if feeding out)
Kilowatt loss $=+1.10B^2+1.01W^2+1.22N^2+0.70M^2+4.47A^2+0.73BW-1.31BN-0.52BM-0.24BA-1.56WN-0.78WM-0.12WA+0.62NM-1.42NA-0.62MA$

45. The major and minor axes of the ellipses are seldom parallel to the co-ordinate axes. The angle of one axis of the ellipses with respect to the B co-ordinate may be determined by the following expression (but the sign of the angle and determination of whether major or minor axis should be checked by calculation of a few points from the loss equation):

$$\tan=0.5\tan^{-1}[L_{AB}\div(L_{BB}-L_{AA})]$$

L should be replaced by K if the first form of equation is used.

46. Another important determination is the location of the center of the loss ellipses. This center is also the point of minimum loss, unless the center is at the origin of co-ordinates—in which case the minimum loss must be determined by other means. The co-ordinates of the center are as follows:

$$A_R=\frac{2L_AL_{BB}-L_BL_{AB}}{4L_{AA}L_{BB}-L_{AB}^2}$$

$$B_R=\frac{2L_BL_{AA}-L_AL_{AB}}{4L_{AA}L_{BB}-L_{AB}^2}$$

47. In the first form of equation (consisting of K terms instead of L terms, the loss ellipses are concentric about the origin of co-ordinates, and A_R and B_R are each equal to zero.

48. The next items to be determined are the major and minor radii. There will be a pair of these for every value of loss locus to be plotted. To determine these radii, make the following substitutions:

$$F=L_A^2L_{BB}+L_B^2L_{AA}-L_AL_BL_{AB}$$
$$G=4L_{AA}L_{BB}-L_{AB}^2$$

49. Minimum loss occurs when:

Kw loss $=L_C-(F\div G)$

In item 31, x equals minimum loss (then unknown).

50. Continue the substitutions begun in item 48:

$$P=L_{AA}+L_{BB}-\sqrt{(L_{AA}-L_{BB})^2+L_{AB}^2}$$

$$Q=L_{AA}+L_{BB}+\sqrt{(L_{AA}-L_{BB})^2+L_{AB}^2}$$

$$R_L^2=(\text{major radius})^2=\frac{(F\div G)+(\text{kw loss})-L_C}{0.5P}$$

$$R_s^2=(\text{minor radius})^2=\frac{(F\div G)+(\text{kw loss})-L_C}{0.5Q}$$

51. An ellipse can be constructed most easily and with adequate accuracy by using three radii (see pages 2–51 of "Handbook of Engineering Fundamentals" by Eshbach). These radii have been determined analytically by substitution of the standard formula for radius of curvature of an ellipse (see page 152 of "Calculus" by Granville, Smith, and Longley).

52. For each locus of constant loss, determine the following (in addition to R_l and R_S):

At the ends of the major axis (center on the major axis)

$$R_C=R_s^2\div R_L$$

At the ends of the minor axis: (center on the minor axis)

$$R_C=R_L^2\div R_s$$

At some intermediate arc (length and center to be determined by trial)

$$R_C=\sqrt{R_LR_s}$$

See Figure 1 for a typical loss diagram on rectangular co-ordinates. The ellipses are loci of constant loss, with all sources except two base-loaded. Instead of using the two sources as co-ordinates, the total territorial load of the system is used as one co-ordinate, and one source as the other.

53. Minimum-loss co-ordinates as obtained in item 46 should be checked by the results of item 36.

54. Minimum loss in kilowatts as obtained in item 49 should be checked by substituting the co-ordinates in item 36 in the loss equations of item 15.

55. Loss values at several diverse points on a chart constructed as per items

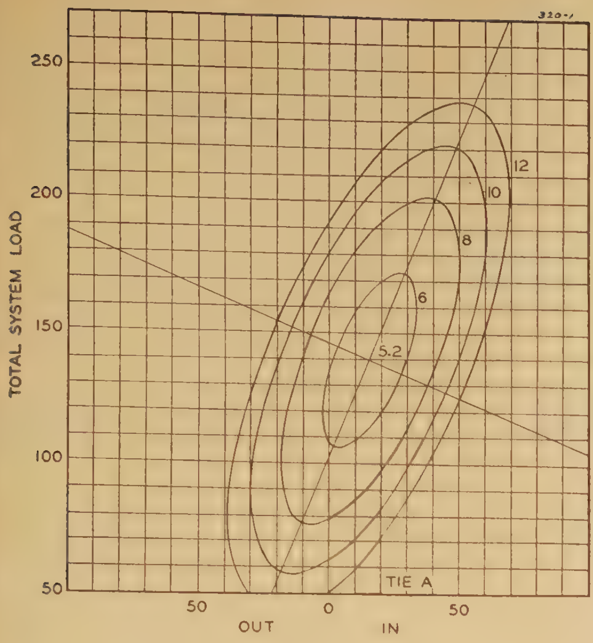

Figure 1. Typical loss chart

System load, tie-line load, and loss figures are in megawatts. Minimum loss is 5.2, at center of ellipses

41 to 52 should be checked by comparing values computed from the loss equation of item 15.

Application of New Method to A-C Calculating-Board Studies

56. The superposition method has been used to date for operating purposes, where the use of the a-c calculating board (except for determining load division) would be impractical. There has been no opportunity as yet to apply the superposition method in a-c board studies. If kilowatt and kilovar load flows were read and totalled separately, the loss equation would take account of generator excitation, charging current, synchronous condensers, different load power factors, different R/X ratios, and so forth. True vector relations would be maintained, and it would be possible to determine definitely the accuracy and limitations of the longhand scalar method. This proposed combination of a-c calculating-board analysis with the superposition method is largely an unexplored field.

Time Requirements of New Method

57. Power systems differ widely in the time required to calculate loss formulas, as well as in the time required to use them. On a system involving five sources (plants and tie lines) but with only a few loops, a loss formula was calculated completely and checked by two men in two days. Other systems with eight to ten sources and complicated networks have required two or three weeks work by two men to calculate the loss formula. If a d-c or a-c calculating board is available for load division, the balance of the work in determining the loss formula can easily be done in N^2 man-hours where N is the number of sources.

58. The use of the loss formula for a given set of operating conditions may require one minute or 15 minutes, depending on the number of variable sources. A large number of operating conditions can be calculated very quickly, if the data are set up in tabular form and carried out systematically.

59. In general, a loss formula can be worked out in less time than data could be calculated and tabulated for an a-c board study. Losses for a number of conditions can then be calculated in a small fraction of the time required to read losses on the calculating board, not to mention the time to change board setups.

Accuracy of New Method

60. The accuracy of loss calculations by the superposition method outlined herein is estimated to be better than ten per cent plus or minus on single readings and better than five per cent plus or minus on daily totals—even under difficult conditions. Since losses are only 2 per cent to 15 per cent of power transfer, errors in determining transmission losses are comparable with errors customarily tolerated in metering input and output to the transmission system.

61. The use of the scalar value of Z in determining load division without regard to the R/X angle has a negligible effect on accuracy, as may be seen by calculating a few typical cases (determine 1 in loop branches with Z constant and R/X variable).

62. Loss data on typical transmission systems show that incremental losses cannot be approximated satisfactorily by taking total losses as proportional to the square of the system load. Prior to the development of the superposition method, this was attempted, using a peak loss determined from the annual load factor and the annual kilowatt hour loss (as determined from monthly generation, tie-line, and primary-substation meter readings), following the procedure described on page 1159 of the seventh edition of the "Standard Handbook of Electrical Engineers." This square law relation for total loss applies only to systems with one power source or with several sources all loaded uniformly (on the same percentage basis).

63. On the usual transmission system, the curve of total losses against total system load may change direction abruptly whenever load is placed on a new plant. Each change in regulating plant means a change in the shape of the loss curve. The total loss curve usually consists of segments of parabolas (one regulating source) or ellipses (more than one regulating source) with a discontinuity at each junction.

64. Incremental intrasystem transmission loss may be either positive or negative.

65. The possible sources of error in this method are:

A. The product of voltage and power factor may not equal the assumed average value. However, most of the line loss occurs near the sources, and the average voltage and

Table IV. Summary of Minimum-Loss Determinations

I. Minimum-Loss Loadings (in Per Cent of Net System Input)

	Burton	Washington	Nelson	Madison	Anderson
5 sources	17	28	30	17	8
4 sources	19	31	30	20	*
4 sources	20	30	41	*	9
5 sources	23	34	43	*	*

II. Megawatts Loss for 100 Megawatts Net System Input With Above Minimum-Loss Distribution**

	Megawatt Loss
5 sources	0.38
4 sources	0.67
4 sources	0.61
3 sources	1.00

* Supply at this source equals zero.

** Total loss varies as the square of the net system input.

line power-factor conditions should be determinable with fair accuracy.

B. The product of voltage and power-factor changes with substation loads, interconnection transfers, generator kilowatt loading, and generator reactive loading. On systems with large charging current, the uncorrected calculated losses will be too high at times of light generation and low interchange. An estimated correction is desirable in such cases.

C. The principle of superposition applies to a-c load flows only if the line power factor is uniform and constant. Short-circuit determination on the d-c calculating board and other data indicate that the error due to arithmetic addition of currents from various sources is not important on systems with reasonably uniform R/X ratios on major tie lines and with reasonably uniform power factors on major plants.

D. Losses are neglected in determining the synthetic load-flow diagrams. Analysis indicates that losses do not greatly change the load division near power sources—where the losses are important. Furthermore, the calculated currents near the sources would not be reduced much if line losses were subtracted.

E. The percentage distribution of kilowatt load between substations changes hourly and seasonally, but the net effect is ordinarily small, as may be seen by comparing peak and off-peak board studies of typical systems.

F. The prorating of small substation loads at terminal substations causes the calculated losses to be too small, but this error can be reduced by using a more complete transmission diagram.

G. Line loss is calculated on the basis of uniform current throughout.

66. Errors listed under *A*, *C*, *D*, and *G* are eliminated if an a-c calculating board is used to determine the load flows from each source (provided kilowatt and kilovar flows are tabulated separately). The use of an a-c calculating board will facilitate determination of loss equations for two or more conditions, such as peak and off-peak, reducing the errors listed under *B* and *E*.

67. Even when using longhand calculation, losses under the most important operating conditions can be determined with reasonable accuracy, using a single loss equation (with three to six variables, depending upon the number of major source areas).

Appendix A. Derivation of Loss Formula

Each megawatt flow in a given line in item 8 for a given source multiplied by the ratio of actual generation at that source to 100 megawatts would give the actual megawatts in the line. The total megawatt flow in each line is as follows designating the various megawatt generator and interconnection loadings as G_A, G_B, G_C, and so forth, and the flows—on a 100-megawatt or per cent basis—as F_A, F_B, F_C, and so forth:

$$\text{Total flow in megawatts} = (G_AF_A + G_BF_B + G_CF_C + \text{ and so forth) divided by 100 per cent}$$

At a given transmission voltage and given power factor, amperes are linearly proportional to megawatts, and a common constant factor H may be applied to the flow totals to get three-phase kilowatt loss. Let kv be the average phase-to-phase voltage (in kilovolts) for the heavily loaded portions of the transmission system (usually near generating plants). Let PF be the average decimal power factor in the heavily loaded portions of the transmission system.

$$\text{Three-phase transmission loss in kilowatts in any line} = (3)(I^2R)(0.001) = (0.003)(I^2R)$$

$$\text{Loss in any line} = \frac{(0.003)(\text{kva})^2(R)}{(1.73)^2(\text{kv})^2} = \frac{(0.001)(\text{kw})^2(R)}{(PF\times\text{kv})^2} = \frac{(1{,}000)(\text{mw})^2(R)}{(PF\times\text{kv})^2}$$

$$\text{Loss in any line} = \frac{(1{,}000)(G_AF_A + G_BF_B + \text{ and so forth})^2(R)}{(PF\times\text{kv})^2(100\text{ per cent})^2} = \frac{(0.1)(G_AF_A + G_BF_B + \text{ and so forth})^2(R)}{(PF\times\text{kv})^2}$$

$$H = \frac{(0.1)}{(PF\times\text{kv})^2}$$

$$\text{Loss in any line} = H(G_AF_A + G_BF_B + \text{and so forth})^2(R)$$

$$\begin{aligned}\text{Intrasystem loss in kilowatts} &= H\Sigma[(G_AF_A + G_BF_B + \text{ and so forth})^2(R)] \\ &= H\Sigma[(G_A{}^2F_A{}^2 + G_B{}^2F_B{}^2 + 2G_AF_AG_BF_B + \text{and so forth})(R)] \\ &= HG_A{}^2\Sigma(F_A{}^2R) + HG_B{}^2\Sigma(F_B{}^2R) + \text{ and so forth} + 2HG_AG_B\Sigma(F_AF_BR) + \text{ and so forth}\end{aligned}$$

$$K_{AA} = H\Sigma(F_A{}^2R)$$

$$K_{AB} = 2H\Sigma(F_AF_BR)$$

Making the above types of substitutions gives the general loss formula in item 15.

The square terms will always be positive, regardless of the direction of flow in the line, and regardless of whether the plant or interconnection is delivering or receiving power. The product terms will be either positive or negative, depending upon whether the basic flows from the two sources are in the same direction or not, and upon whether power is being delivered or received at each of the two sources.

If N is the number of sources, the number of square terms will be N. The number of product terms will be $(N^2-N)\div 2$ (see Table I).

Appendix B. Procedure for Constructing Triangular Loss Charts

If only three variable sources are involved, a triangular co-ordinate type of loss chart may be constructed from the loss equation derived in item 15.

$$\text{Kw loss} = K_{AA}A^2 + K_{BB}B^2K_{CC}C^2 + K_{AB}AB + K_{AC}AC + K_{BC}BC$$

Assume a fixed total-system net input. This permits elimination of one variable source (preferably the largest one). Substitute:

J = total-system net input
$C = J - A - B$
$C^2 = J^2 + A^2 + B^2 - 2JA - 2JB + 2AB$

After completing the substitution for J, let $J = 100$ megawatts. This results in an alternative form of loss equation with one source eliminated.

$$\text{Kw loss} = M_{AA}A^2 + M_{BB}B^2 + M_AA + M_BB + M_{AB}AB + M_C$$

$$M_C = 10{,}000\,K_{CC}$$

The preceding equation would be an ellipse if A and B were measured *along* rectangular axes. To make a graph of the preceding equation on triangular co-ordinate paper, it is necessary to measure the preceding values of A and B *from* triangular axes. The shape of the resulting figure is to be determined from a new equation derived by transforming co-ordinates. The usual geometrical methods of constructing curved figures representing second-degree equations assume the use of rectangular co-ordinates. Denoting by subscripts whether triangular (T) or rectangular (R) co-ordinates are used:

$$M_{AA}A_T{}^2 + M_{BB}B_T{}^2 + M_AA_T + M_BB_T + M_{AB}A_TB_T + M_C - \text{kw loss} = 0$$

Substitute

$A_T = 0.866B_R - 0.500A_R$
$B_T = A_R$

This substitution results in another ellipse, referred to rectangular co-ordinates, and therefore easy to construct when the major and minor radii, location of center, and slope of major axis are known. The last two items are the same for all values of assumed loss.

$$N_{AA}A_R{}^2 + N_{BB}B_R{}^2 + N_AA_R + N_BB_R + N_{AB}A_RB_R + N_C - \text{kw loss} = 0$$

From this point on, the construction of elliptical loci on triangular co-ordinates is the same as outlined for the L type of equation in item 41, except that the N values are used. Minimum-loss co-ordinates on triangular co-ordinate paper will be at the center of the loss ellipses. These co-ordinates may be checked as follows:

$$\frac{\partial\text{ loss}}{\partial A_T} = 2M_{AA}A_T + M_{AB}B_T + M_A = 0$$

$$\frac{\partial\text{ loss}}{\partial B_T} = M_{AB}A_T + 2M_{BB}B_T + M_B = 0$$

Solution of the above equations will give the minimum-loss co-ordinates and check the center of the loss ellipses on the triangular-co-ordinate diagram.

The slope of the ellipse axes is best referred to the B_R axis by using the N equation.

In general, quantities derived by the conventional formulas of analytic geometry require the use of the N equation and rectangular co-ordinates, while quantities plotted as triangular co-ordinates require the use of the M equation.

TRANSACTIONS SECTION

Preprint of Corresponding Pages From the Current Annual AIEE Transactions Volume

Any discussion of these papers will appear in the June 1943 Supplement to Electrical Engineering—Transactions Section

Metering Power on the Low-Voltage Side

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POWER consumers often are supplied from high-voltage distribution or subtransmission circuits through transformer banks. When such consumers are to be charged on the basis of high-voltage or primary supply, some way of including the transformer losses is desirable without resorting to the difficulties and expense of direct primary-circuit metering.

Where metering is applied on the low-voltage side of the power transformers, errors introduced in the high-voltage input values are due to the losses in the transformers, and are of two kinds:

1. Core losses: These are very closely proportional to the square of the voltage and independent of load. The core losses consist of hysteresis and eddy-current losses in the iron. These losses are also affected by frequency, but the variations in frequency on modern systems are so small that the effect is negligible. Changes in temperature likewise have too small an effect to be considered.

2. Copper losses: These are proportional to the square of the current. Copper losses may be divided into two parts; namely, the ordinary I^2R losses, such as would be caused by resistance R, and the eddy-current losses. The temperature coefficient of the former is positive, while that of the latter is negative. Since the I^2R loss is the larger, the net effect is that losses increase with temperature. A temperature rise of 50 degrees centigrade increases the losses by about ten per cent, whereas the resistance of copper changes by roughly 20 per cent over this range. Actually, the effect of temperature changes can be disregarded in the present discussion, since it is a small percentage of the power being measured. Some of the methods to be given can be made to compensate easily for temperature changes, but this is more of a theoretical advantage than a practical one.

Transformers of the size used in serving consumers from medium-voltage lines may be about 99 per cent efficient at full load, with roughly equal division of the losses, or one-half per cent core loss and one-half per cent copper loss. In some cases, however, the copper loss at full load may be almost twice as high as the core loss.

There are two different metering arrangements in common use:

1. Current transformers on the high side and potential transformers on the low side.
2. All instrument transformers on the low side.

Arrangement 1 will measure the core loss but not the copper loss. Arrangement 2 does not include any transformer losses.

Various[1] methods may be used to include the effect of the transformer losses. Some are:

1. Estimation of losses from the meter readings.
2. Loss or compensating meters may be added to measure voltage squared and current squared, which, by proper calibration, will read core loss and copper loss, respectively.
3. Line-drop compensators may be used to correct for transformer resistance drop and so include copper loss.
4. Dummy loads may be added to instrument transformer secondary windings to correct for core loss.
5. Methods 3 and 4 may be combined, using compensators for copper loss and dummy loads for core loss.
6. Light-load and full-load meter adjustments may be altered to shape the load curve to the reciprocal of the transformer efficiency curve.
7. Methods 3 and 6 may be combined, by using compensators for copper loss and light-load adjustment for core loss.

These methods will be taken up in order for a typical case: The following assumptions are made:

1. The installation is three 200-kva single-phase transformers fed from 13,200 volts.
2. Core loss is 1.1 kw per transformer at rated voltage.
3. Copper loss is 1.9 kw per transformer at rated load.
4. Consumption is 150,000 kilowatt-hours per month. The month is assumed to consist of 730 hours.

From the above information, the core loss at rated voltage is seen to be:

$3\times1.1\times730=2{,}409$ kilowatt-hours

If the average voltage differs from rated voltage, the core loss will be approximately $2{,}409\times(\text{average voltage}\div\text{rated voltage})^2$.

The copper losses can only be approximated. The 150,000 kilowatt-hours could be 206 kw for 730 hours, or 600 kw for 250 hours, or many other combinations of load and time between these limits. The figure of 600 kw is set as the probable upper limit of the load, since any higher load might damage the transformers.

206 kw for 730 hours, the lowest possible loss for this registration, gives $3\times1.9\times730\times(206/600)^2=490$ kilowatt-hours

600 kw for 250 hours, the highest probable loss, gives $3\times1.9\times250=1{,}424$ kilowatt-hours

The customer ordinarily would not install an excessively large transformer bank, nor would he dangerously overload it. Consequently, the loss limits are set, the former condition giving a lower loss limit (core loss plus continuous-load copper loss), and the latter giving the upper loss limit.

The various methods will now be discussed in detail.

1. Estimation of Losses From the Meter Readings

The increase in the energy rate to cover losses should be such that the total

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energy per month, multiplied by the increase in rate, will equal the total transformer losses over the month multiplied by the usual energy rate. Expressing this as an equation, let

E = energy metered on secondary winding side per month
L = transformer losses per month
R = usual energy rate
ΔR = rate increase

Then

$$\Delta R \times E = L \times R$$

or

$$\Delta R = R \times L/E$$

The percentage increase is evidently L/E which in most cases will be perhaps two per cent as a maximum. Two per cent would be too great at full load, but this compensates for the fact that no energy is being registered at no load, although core loss is still being supplied. The approximation is quite close, as will be seen.

The demand rate should be increased by about one per cent to cover losses at full load (expected demand).

The previous reasoning also makes it plain that discounts may be reduced by the same percentages in order to compensate for the losses; namely, two per cent for energy and one per cent for demand. The estimation below shows how this will work out.

The losses might also be estimated and billed separately at the usual rate.

The estimation in any event would be as follows:

Energy metered = 150,000 kilowatt-hours

Core loss (one-half per cent of transformer rating for 730 hours) is $730 \times 1 \times 3 = 2{,}190$ kilowatt-hours

Copper loss (assume one-half per cent of kilowatt-hours) = 750 kilowatt-hours

Estimated total losses = 2,940 kilowatt-hours

Total energy billed = 152,940 kilowatt-hours

As previously mentioned, there are two limiting conditions:

(*a*). A steady load all month which gives minimum losses.

In this case, the steady load which would deliver the given energy in one month would be (150,000/730) = 206 kw.

Copper losses would be 490 kilowatt-hours, as calculated previously. Total losses would be 2,680 kilowatt-hours. Total energy supplied would be 152,680 kilowatt-hours.

(*b*). Rated load on for a length of time which delivers the given energy, and no load the remainder of the time.

Thus, 600 kw for 250 hours would give 150,000 kilowatt-hours. The copper losses under these conditions would be 1,424 kilowatt-hours as calculated previously. Total losses would be 3,614 kilowatt-hours, and total energy supplied 153,614 kilowatt-hours.

The estimate is 2,940 kilowatt-hours, whereas the average of the two limiting cases is 3,147 kilowatt-hours. The billing based on the estimation would be within 0.15 per cent of the billing based on the average of the two limiting conditions. Actually, this error is less than those encountered in meter calibration.

The total losses are seen to be just about two per cent of the measured power in either case, which is the justification for the two per cent figure used above.

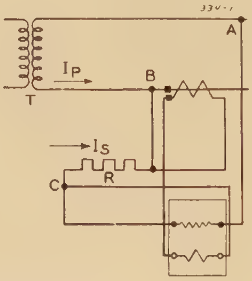

Figure 1 (left). Line-drop compensator—single-phase circuit

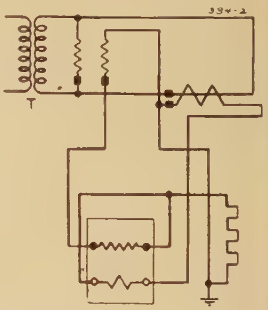

Figure 2 (right). Line-drop compensator—single-phase circuit with alternative connections

Even the limiting conditions above give errors of only 0.2 per cent and 0.45 per cent, and these conditions are very improbable. For case *b* (maximum losses), the estimate would be within 674 kilowatt-hours of the actual losses. At 1.5 cent per kilowatt-hour, this would mean a loss of revenue of $10 a month or $120 a year. A metering outfit for use on the high side would cost over $1,000. Interest, taxes, and depreciation would take most of the revenue collected, showing little justification for the use of a metering outfit on this size of load.

Another billing method, not quite so simple as the first given but slightly more accurate, is to add a fixed number of kilowatt-hours, plus a percentage of the energy registered. The fixed number of kilowatt-hours would equal the core loss as previously estimated. The percentage of registered energy would be (copper loss/registered energy) also as previously estimated. This method is obviously the same as estimating the losses and billing at the usual rate.

Also, in general, it is true, that, the higher the registration for a given transformer rating, the more accurate the estimate can be made.

2. Loss or Compensating Meters

Since core loss varies as the square of the voltage, a voltage-squared hour meter consisting of one element for each phase of the transformer will register the core loss if the correct multiplier is used. Similarly, copper loss may be measured by a current-squared meter with the correct multiplier. The error in loss measurement can be reduced to a very small amount, but accurate transformer data are required, and these are not always available or obtainable. Transformer data can be estimated, but design variations are such that the error may be as much as 0.3 per cent of the total power, and the accuracy is not much better than the estimating methods given. When the data are accurately known, the errors generally will be no greater than those involved in metering directly on the high-voltage side and, as will be seen later, may be less.

Compensating meters may be individual current-squared and voltage-squared hour meters, or they may be combined in one meter. Although three elements of each type theoretically would be required, the accuracy is almost as good using one voltage and two current elements.[2] If the load is normally well balanced, one current element is sufficient.

Loss meters also may be used to form the basis for an estimating method. That is, a compensating meter may be installed for a representative period of time, and the losses compared to the registration of the watt-hour meter during that period. This may be used then as a basis for changing the rate. For example, if the losses were 1.75 per cent of the registration during this period, the energy rate evidently should be increased, or the discount reduced, by 1.75 per cent. Loss meters of the current-squared type only would be sufficient in cases where the voltage is known fairly accurately. Most installations probably will fall into this category. A five per cent change in voltage will change core loss by about ten per cent. However, core loss will normally be about one per cent of the registration, so that the net error would be only about 0.1 per cent, if the average voltage differed by five per cent from that estimated.

3. Line-Drop Compensator

Impedances may be used in the secondary windings of current transformers to produce a drop equivalent to that of the transformer bank. This drop may be added to the secondary potential transformer voltage to make a sum proportional to the primary voltage. Since a reactance drop would be at right angles to the current, the added voltage from a reactance-drop compensator would produce no registration on a watt-hour meter. Therefore, only resistance compensation is needed in power metering.

The type of transformer bank determines the number of compensators required; two for open delta, three for star or delta.

Figure 1 illustrates the principle of the line-drop compensator. The current transformer circulates the current I_s in the direction indicated, while I_p proportional to it flows in the main circuit. The voltage between *B* and *A* must be increased by the resistance drop in the transformer *T* to obtain the equivalent high-side voltage. The resistor *R* is adjusted so that the secondary current produces a drop equivalent to the transformer drop. It is seen that I_p and I_s both flow toward *B*, so that the polarity is such as to add the voltage drop.

Another way of looking at this is to say that voltage *BA* would normally be applied to the potential coil, *B* being at higher potential than *A* when the current flow is as indicated. The point *C*, however, is at higher potential than *B* by the amount of the resistance drop in *R*. Therefore, if the potential coil is connected from *C* to *A*, the potential across it will be greater than *BA* by the drop in *R*. The drop in *R*, in turn, is equivalent to the drop in *T*, so that the equivalent of the high-side voltage is applied to the meter.

Figure 2 shows the same method, rearranged to permit grounding of instrument transformer secondary windings. The difference from Figure 1 is in the order in which the transformer secondary compensating resistor and meter coil are connected in series.

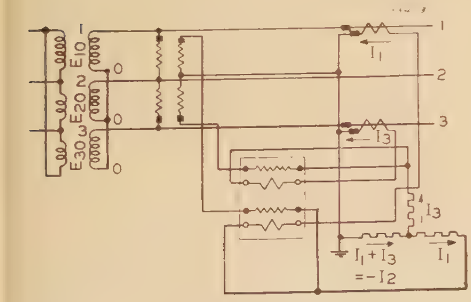

Figure 3. Line-drop compensator used in three-phase three-wire circuit

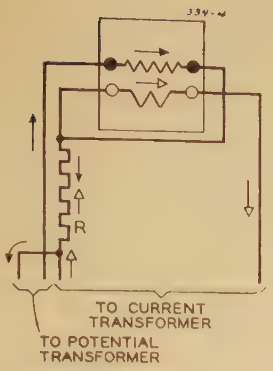

Figure 4. Polarity requirements for line-drop compensators

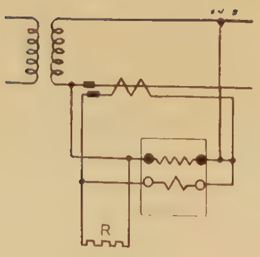

Figure 5. Connection of dummy loads to measure core loss

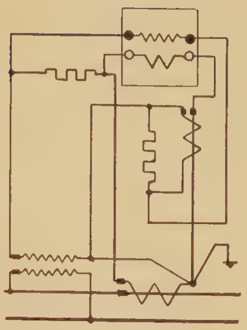

Figure 6. Combination of line-drop compensator with dummy loads using an additional current transformer, single-phase circuit

In Figure 2 the current flows through the compensator and then through the meter. The potential coil is connected across *DF*. The voltage *DE*, which normally is applied to the meter, is increased by the drop *EF* and the total *DF*, applied to the coil. *E* is at a lower potential than *D*, and the drop in *EF* places *F* at a still lower potential than *E*.

Figure 1 is the clearest for single-phase, whereas Figure 2 is simpler in a three-phase circuit, in which a third resistor is needed to compensate for the third phase current as shown in Figure 3.

Note that although only two elements are used, the drops in the three transformers are taken care of by the wye-connected compensator. In Figure 3 the voltage E_{12}, for example, $=E_{10}+E_{02}=E_{10}-E_{20}$. The drops which are added vectorially to this voltage before applying it to the potential coil are the ones caused by the currents I_1 and $-I_2$. ($I_1+I_3=-I_2$.) The addition of these drops gives the equivalent high-side voltages E_{10} and $-E_{20}$, which equals E_{12}.

Although the transformers are shown connected delta-wye, the secondary of course, could be delta-connected, and the meter and compensator connections would not be changed.

The polarity of the connections on a line-drop compensating scheme is important. To add a voltage to correct for *IR* drop, a drop $-IR$ is needed. This is obtained by connecting the voltage leads so that the currents of the voltage and current circuits are opposite in the compensator (see Figure 4).

Sometimes the wiring may be used to provide the resistance required. For example, with one-half per cent drop, or 0.6 volt on 120-volt basis, the value of *R* would be about 0.12 ohm at five amperes or say 75 feet of number 12 American wire gauge wire. Where common return wires are used for both current and voltage, a compensating effect occurs. With the usual connection, it results in low readings, since the currents of the voltage and current circuits are in the same direction. When properly connected, however, compensation may be obtained this way without other equipment.

4. Dummy Loads

Dummy loads may be used to correct for core loss. The saturation curve of the core is not a straight line, with the result that the correction will be slightly low on higher than normal voltage, and slightly high on lower than normal voltage.

Figure 5 shows core-loss compensation by means of a dummy load (the resistance *R*), which allows a current to circulate through the current coil in such a direction as to add the core loss. The value of *R* usually will be around 5,000 ohms. The saturation curve could be approximated by an iron-cored impedance *Z*, but adjustment difficulties make this impractical.

5. Line-Drop Compensators and Dummy Loads Combined

Methods 3 and 4 may be combined to correct for copper loss and core loss, but it will be found that the voltage and current connections required are not consistent with each other if both the transformers are to be grounded.

Four alternatives exist:

(*a*). Use additional current transformers to reverse the current for the resistor compensating for copper loss (Figure 6).

(*b*). Use additional voltage transformers to reverse the voltage for the resistor compensating for core loss (Figure 7).

(*c*). Add an extra current winding on the meter to provide the equivalent of *b* (Figure 8).

(*d*). Use grounds on only the current transformer secondary windings. The voltage

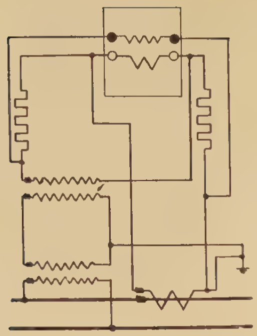

Figure 7. Combination of line-drop compensator with dummy loads using an additional potential transformer, single-phase circuit

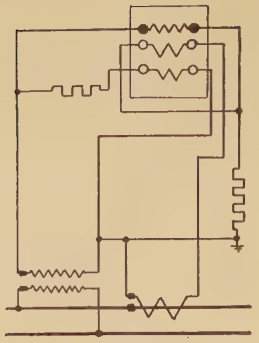

Figure 8. Combination of line-drop compensator with dummy loads using an additional series winding, single-phase circuit

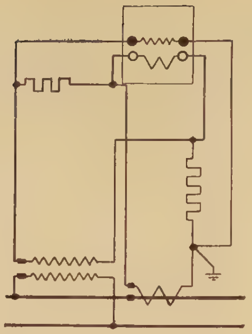

Figure 9. Combination of line-drop compensator with dummy loads, grounding the potential transformer through the current circuit, single-phase circuit

transformer secondary windings will then be grounded through the current circuit[8] (Figure 9).

6. Meter Adjustments

This method consists of setting the meter about five per cent fast at ten per cent load, one per cent fast at full load, and two per cent fast on 50 per cent power factor. Since the effect of the adjustments practically will offset the transformer losses, the over-all result will be quite close over the ranges of load normally encountered.

From the transformer efficiency curve, the full-load and light-load settings can be determined. In the example shown in Figure 11, these values are found to be 1.5 per cent at 100 per cent load and 5.5 per cent at 10 per cent load. In this case, the resultant curve is fairly good over the whole range. If the transformers are under no load a fairly large portion of the time, it may be advisable to use a meter without anticreep holes or slots in the disk so that it will creep with voltage only applied.

If the disk has no irregularities of any sort, the core loss will be measured with very good accuracy, since the light-load adjustment provides a torque which varies as the square of the voltage, as does the core loss.

Note, however, that the same uniformity of the disk would be required of a meter on the high side, if the core loss were to be measured during periods of no load. Therefore, metering on the high side may be less accurate than some of the methods described here.

The method of determining the desired curve is as follows:

Take the reciprocal of the transformer efficiency curve. This is the curve which the watt-hour-meter registration should match. The standard watt-hour-meter curves are then changed by three adjustments. Full-load adjustment changes all points equally. Lagging adjustment changes all points at any given power factor (say 50 per cent) equally, and intermediate power factors are affected almost proportionally. For example, calibrating 100 per cent load to 101 per cent at unity power factor and 102 per cent at 50 per cent lagging power factor would cause all loads at unity power factor to register one per cent faster than the standard curve. All loads at 50 per cent power factor would register two per cent faster than the standard curve for 50 per cent power factor. At 75 per cent lagging power factor the change would be about 1.5 per cent.

The light-load adjuster supplies a torque independent of load, and therefore the effect on registration varies inversely with the speed. A change of five per cent at ten per cent load would change 50 per cent load by one per cent. Note that 50 per cent power factor will be affected twice as much on a percentage basis as unity power factor at the same current.

By means of these adjustments, the inverse of the transformer efficiency curve usually may be approximated rather closely. In this method, as well as any of the others, the transformer data are helpful in increasing the accuracy. If they are not obtainable, an average characteristic must be assumed.

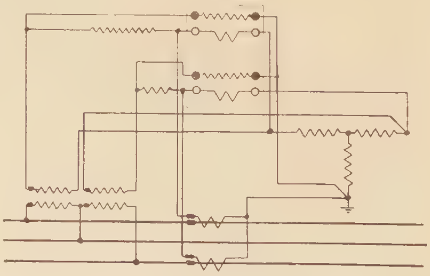

Figure 10. Combination of line-drop compensator with dummy loads, grounding the potential transformer through the current circuit, three-phase circuit

7. Combination of Meter Adjustment and Line-Drop Compensators

Line-drop compensators may be used for copper-loss compensation, and fast light-load adjustment for core-loss compensation. This would be more accurate on unbalanced loads, such as arc furnaces, than method 6, although the advantage is not great.

Reactive Power Measurements

The foregoing applies to power measurement. However, the same methods may be applied to the measurement of reactive power.

The errors introduced are, in general, the same as those involved in power measurement as previously discussed.

The reactive power losses again may be divided into two classes:

1. Excitation losses (corresponding to core loss)—dependent on voltage, but varying much more rapidly than the square.

2. Leakage reactance loss (corresponding to copper loss)—proportional to the square of the current.

Transformers of the type in question will have reactive power losses ranging from two per cent to ten per cent, depending on the transformer design.

The methods which were used for power metering may also be used to include reactive losses. The following points, however, should be noted.

When using line-drop compensators,

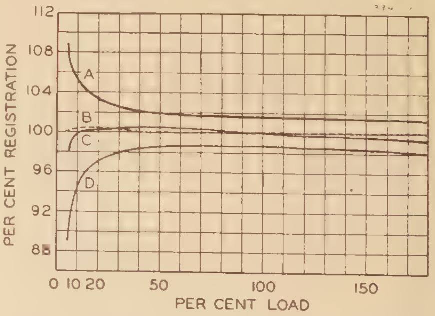

Figure 11. Curves illustrating the use of meter adjustments to approximate the reciprocal of transformer efficiency

A—Meter curve calibrated 1.5 per cent fast at 100 per cent load, 5.5 per cent fast at 10 per cent load

B—Meter curve at normal calibration

C—Meter registration in terms of transformer input

D—Transformer efficiency curve

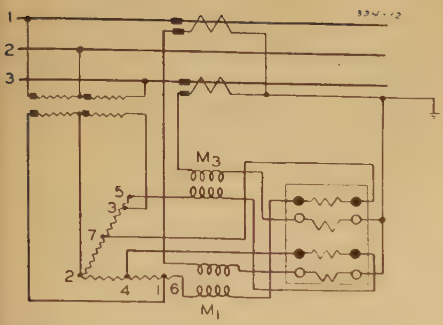

Figure 12. Line-drop compensation used on three-phase reactive meter circuit

reactance only is needed, for the same reason that resistance only is required to measure conductor loss. That is, a resistance drop added to the potential of the reactive meter would produce no rotation. Note, however, that when the reactive meter consists of a watt-hour meter and phase-shifting transformer, the above statements apply only when the compensation is applied on the primary side of the phase-shifting transformer. On the secondary side, resistance compensation could be used, but difficulties in making the proper connections made this undesirable. The best method is probably that shown in Figure 12 which used mutual inductors. This allows more freedom in making connections, since the two windings are not tied together.

It will be observed that the current transformer in line 1 feeds current through mutual inductance N_1 which is in the potential circuit 6–7. This voltage lags 3–2 by 90 degrees and is applied to the element in which the current from the transformer in line 3 is used. The connections of the other inductance are similar.

The use of dummy loads to measure kilovar-hour excitation loss gives somewhat less accuracy than the use of dummy loads for core loss.

If compensating meters of the voltage-squared and current-squared type are used to measure power losses, no additional equipment is necessary to measure reactive power losses. Multiplying the readings of these meters by the correct factors will give the reactive power losses.

Reactive power measurements are subject to errors, because the magnetizing current varies widely with fluctuations in voltage. For this reason, the most accurate method is the use of compensating meters of the voltage-squared type. The kilovar-hours can then be computed from the readings obtained. This method probably is justified only on transformers with high magnetizing current. If the reactive power measurement is used only to determine demand, the errors in any method will be rather small.

Summary

The foregoing will make it plain that several methods of including transformer losses in the billing are available. Both estimating and measuring methods may be used with reasonable accuracy. There is no best method for all installations. Each must be considered individually. The most desirable method is determined by the various factors involved in each installation. These factors will include expense, customer relations, commission requirements, and importance of maintaining laboratory routine, to mention a few of the more important.

Appendix

The fact that the line-drop compensation shown in Figure 3 is correct is proved as follows:

In any delta transformer bank with balanced impedances (Figure 13)

$$\begin{aligned} I_1 &= I_b - I_c \\ I_2 &= I_c - I_a \\ I_3 &= I_a - I_b \end{aligned} \qquad (1)$$

(Since the impedances are balanced)

$$I_a + I_b + I_c = 0 \qquad (2)$$

From equation 1 we find

$$I_1^2 + I_2^2 + I_3^2 = 2(I_a^2 + I_b^2 + I_c^2) - 2(I_aI_b + I_bI_c + I_cI_a) \qquad (3)$$

The actual copper loss $= (I_a^2 + I_b^2 + I_c^2)R$

where R is the a-c resistance of each phase of the transformer. In the meter (see Figure 3) the element with current I_1 has a voltage $(I_1 - I_2)R_c$ added to its potential coil, and similarly the element with current I_3 has a voltage $(I_3 - I_2)R_c$ added to its potential coil, so the loss measured,

$$P = I_1(I_1 - I_2)R_c + I_3(I_3 - I_2)R_c$$

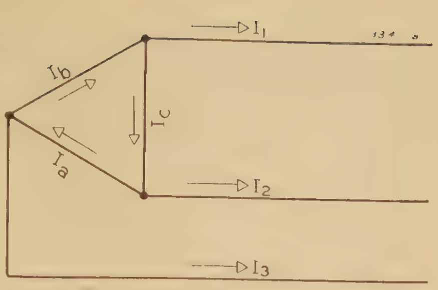

Figure 13. Line and phase currents of the secondary winding of a delta transformer bank

where R_c is the resistance of each leg of the compensator.

Combining terms

$$\begin{aligned} P &= [I_1^2 + I_2^2 - I_2(I_1 + I_3)]R_c \\ &= (I_1^2 + I_2^2 + I_3^2)R_c \text{ since } (I_1 + I_3) = -I_2 \end{aligned}$$

Using equation 3

$$P = [2(I_a^2 + I_b^2 + I_c^2) - 2(I_aI_b + I_bI_c + I_cI_a)]R_c$$

Now we add and subtract

$$(I_a^2 + I_b^2 + I_c^2)R_c$$

which gives

$$P = [3(I_a^2 + I_b^2 + I_c^2) - 2(I_aI_b + I_bI_c + I_cI_a) - (I_a^2 + I_b^2 + I_c^2)]R_c \qquad (4)$$

However

$$(I_a + I_b + I_c)^2 = (I_a^2 + I_b^2 + I_c^2) + 2(I_aI_b + I_bI_c + I_cI_a)$$

From equation 2 this equals 0 so the last two terms in equation 4 drop out. The measured loss is therefore

$$3R_c(I_a^2 + I_b^2 + I_c^2)$$

If

$$R_c = (R/3)$$

the measured loss will therefore equal the actual loss

$$R(I_a^2 + I_b^2 + I_c^2)$$

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The Effect of Current-Transformer Residual Magnetism on Balanced-Current or Differential Relays

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Synopsis: High-speed current-balance relaying has been subject to occasional false tripping that has not been explained satisfactorily. Field and factory tests now show that this is probably due to differences in residual fluxes in the current-transformer cores. The limiting value of these fluxes can be calculated approximately, and they can be measured with portable instruments connected to the secondary winding. They may cause a deficiency in secondary current, especially during the first half-cycle of fault current, which will cause current-balance or differential-overcurrent relays to operate falsely. This false operation can be avoided by the addition of a short inherent or external time delay with relays having a high dropout, or by the use of special current transformers.

BALANCED-CURRENT relaying has been favored widely in system protection, because, in principle, its complete lack of sensitivity to faults outside the protected line makes time delays unnecessary and avoids all problems of co-ordination with the settings of relays further from the source.

Experience with induction-disk relays applied in this form of protection was entirely satisfactory until requirements of system stability or of uninterrupted operation of industrial motors caused a demand for reduction in the fault duration even below the minimum of a few cycles obtained with such relays. High-speed current-balance relays operating in less than a half-cycle were then applied and experience with them also has been satisfactory, generally. However, there has been a very small percentage of false operations resulting in the tripping of a breaker on a sound line, and, if the fault causing this tripping is on the only other line between the two terminals, such false operation naturally causes loss of the tie or interruption of supply. The purpose of this paper is to report an investigation which has shown the probable cause of most, if not all, of the false operations not otherwise accounted for and provided against.

The clew that provided the basis for the investigation was obtained from tests made by G. S. Whitlow, then of the General Electric Company, and C. E. Asbury of the Illinois–Iowa Power Company. They were conducting primary tests with a low-voltage source to determine the reason for one of the false operations mentioned above and found that the only way to cause false tripping with equal primary currents was to reverse the direction of current through the primary winding of one current transformer with respect to the other. The sudden first application of current after a reversal would usually result in operation of one or the other element of the current-balance relay. Since this relay had been tested without current transformers previously with suddenly applied current, they diagnosed the false tripping as being due to residual magnetism in the current-transformer cores. Fortunately, oscillograms were taken during these tests, so the offending element had left some evidence. Figure 1 is a tracing from a typical one of those oscillograms showing the currents which caused false tripping by providing insufficient restraint. The traces show the two secondary currents, and, since the primary conductors of the two current transformers were in series, it is obvious that the deficiency of secondary current in one of the current transformers in the first half-cycle was due to some phenomenon within the current transformer. This phenomenon has been analyzed and will be described briefly at the outset as a basis for an understanding of the test methods and test results described later.

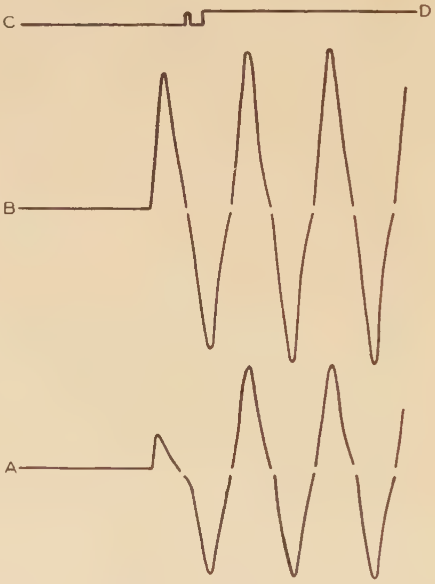

Figure 1. Oscillogram from field tests, showing difference between secondary currents of two similar 38-turn current transformers with similar burdens and 2,540 amperes primary current

2,540/38 = 67

A—Secondary current of current transformer with residual flux

B—Secondary current of current transformer with less (or opposite) residual flux

C—Relay contacts open

D—Relay contacts closed

Hypothesis to Explain the Observed Phenomenon

The following hypothesis appears to be in accordance with the results of the field tests and the factory tests:

1. A significant amount of residual flux is left in the core of only one of the two current transformers which supply one balanced-current relay.

2. This residual flux remains in the current-transformer core practically undiminished unless the circuit is loaded, in which case it is decreased gradually but not usually removed entirely.

3. A subsequent fault beyond (or near) the end of the pair of protected lines with both lines in service will draw equal (or nearly equal) currents through the primary windings of the pair of current transformers as shown in Figure 2. The direction of primary current during the first half-cycle is random and may be such as to require an increase in current-transformer core flux in the same direction as the existing residual flux.

4. The sum of these fluxes is likely to exceed saturation value, in which case there will be a deficiency of secondary current from this current transformer equal to the magnetizing current necessary to produce enough flux to generate the voltage required for the actual secondary current.

5. This deficiency of secondary current will be smaller, possibly even negligible, in the other current transformer because of its lack of residual flux.

6. The greater secondary current from the current transformer without residual flux may operate the balanced-current relay in one direction.

7. Since the location of residual flux (in one

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current transformer or the other) is unrelated to the location of the subsequent fault, and the currents are practically equal except for the transient error, an element may operate falsely for an external fault; or an element on the sound one of a pair of lines may operate for a fault in the other line, near its further end.

With the preceding brief outline to show the relation between the various ideas involved, we can profitably cover the first two of the points in more detail.

1. The residual flux may be left by a half-cycle impulse such as a fault current cleared by an expulsion gap or a fuse, or such as a power-frequency current following a lightning arrester discharge; or by a fault current cleared after an odd number of half-cycles; or by a fault current that is offset because it started in an inductive circuit near the zero point of the voltage wave; or by a power-transformer magnetizing-inrush current which is offset because of the presence of residual flux in the power-transformer core prior to its energization. Residual flux may be left in only one of the pair of current transformers because the primary conductor of its mate is out of circuit at the time of the transient; or because the fault causing the transient was on the primary circuit of one current transformer and so near by that the currents in the two current transformers were greatly different in magnitude.

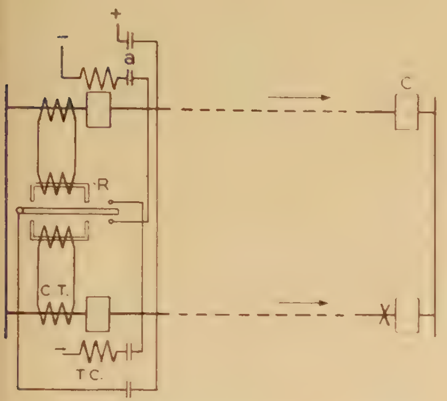
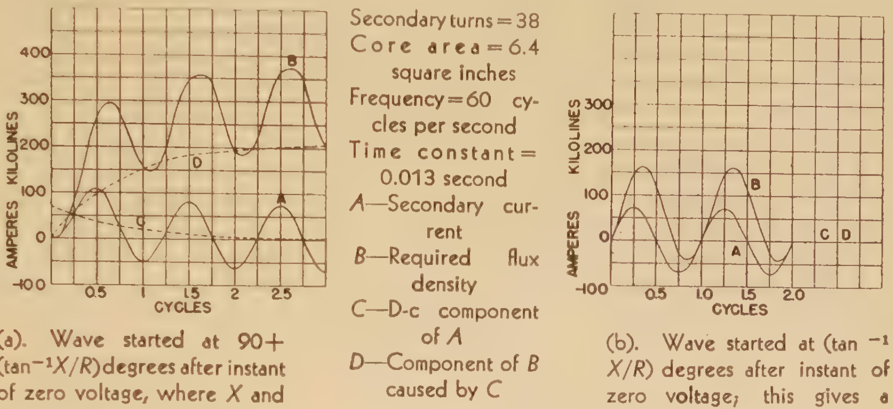


Figure 2. Schematic connections for balanced-current relaying of two parallel lines, showing equal currents in the two lines for a fault at the end of one of them (length of arrows indicates current magnitude)

a = Auxiliary switch, closed when *C* is closed
C = Circuit breaker
CT = Current transformer
R = Current-balance relay
TC = Trip coil
X = Fault

In earlier days of the industry it was considered a serious offense to open-circuit a current-transformer secondary winding partly because of the resulting high voltage, but also because of the danger of magnetizing the core. It is now known that any of the conditions previously mentioned can run the flux density in the current-transformer core up to saturation value if the primary current or secondary burden is high enough, or with smaller currents or burdens if the transient is applied twice or a few times with the same direction of initial half-cycle; and therefore the residual flux introduced by unavoidable circumstances may be practically as great as the value formerly supposed to result only from careless testing or faulty wiring or devices.



(a). Wave started at 90+(tan$^{-1}X/R$) degrees after instant of zero voltage, where *X* and *R* constitute the impedance limiting the current in the primary circuit; this gives a fully offset wave

Secondary turns = 38
Core area = 6.4 square inches
Frequency = 60 cycles per second
Time constant = 0.013 second
A—Secondary current
B—Required flux density
C—D-c component of *A*
D—Component of *B* caused by *C*

(b). Wave started at (tan^{-1} *X*/*R*) degrees after instant of zero voltage; this gives a symmetrical wave

Figure 3. Secondary currents in burden of 0.77+*j*1.06 ohms, and current-transformer-core flux densities required for these currents

Figure 3a shows a fully offset primary-current wave and the corresponding wave of required flux applying to the current transformer and burden used in the error-current tests. This fully offset wave requires more flux than any other current of the same symmetrical value having a smaller degree of offset. The fluxes required for larger or smaller currents are in direct proportion to the current. Figure 3b shows a symmetrical current wave and the corresponding wave of required flux; this symmetrical wave requires less flux than any other current of equal magnitude with this burden. A purely reactive burden of the same total impedance would require a flux wave of the same amplitude, but this flux wave would be symmetrical, and therefore its peak value would be much less. A reduction in secondary resistance may be even more beneficial with an offset wave, because it decreases the unidirectional component of flux caused by the d-c component of current. These flux waves were calculated from the formula

$$\phi=\frac{10^8}{N}\left(R\int_0^t i_p dt+Li_p\right) \qquad (1)$$

2. The fact that normal operation fails to remove the residual flux completely is in accordance with the known characteristics of permanent magnet materials, which differ from transformer iron only in degree even though the degree is extreme. If a large magnetizing force is applied to either of these materials it produces saturation, as indicated in Figure 4, and, if it is reduced to zero without reversal, a residual flux is left, often of just as great a percentage of its own saturation value for high-quality annealed iron as for high-quality permanent magnet steel; the main difference between these steels is in the coercive force necessary to bring the flux to zero.

Then, if a pulsating or alternating magnetomotive force is imposed, and this magnetomotive force is small compared with the coercive force of the particular material, the operation will stabilize on what is known in permanent-magnet theory as a minor loop, and this loop will be far removed from the zero axis of flux, one end coinciding with the residual-flux trace of the high-density hysteresis loop unless the small alternating magnetomotive force has been reduced from the maximum value attained since saturation. A phenomenon of this sort is characteristic of current-transformer operation at normal loads, because they require only a small flux density and therefore a fairly small magnetomotive force even when displaced from the zero axis of flux. Figure 4 is a sketch of half of the usual major loop *B* resulting from the application of fault current, within it a minor loop *C* resulting from the subsequent application of full load current, and also a smaller minor loop *D* obtained by finally reducing the load current to half rated value; these minor loops are not test results, but are intended only to convey an understanding of the phenomenon.

Outline of Tests

In an effort to duplicate in the factory the false operation obtained in field test, a series of tests was laid out using a similar burden on current transformers of nearly the same size as those used in the field tests, and with secondary turns chosen to require the same flux density as those current transformers for the same secondary current. A method was devised, and checked, for measuring residual flux without decreasing it greatly and measurements were made by this method of the residual flux left by various transients. Tests were made to determine whether the passage of time or of rated current would cause a decrease in the residual flux left in the core. Oscillograms were taken showing primary current, secondary current, and error current with and without residual flux, and others were taken showing primary and secondary current and relay operation with and nonoperation without residual flux.

Table I lists for reference the dimensions and other pertinent data on the current transformers and burden used in the tests.

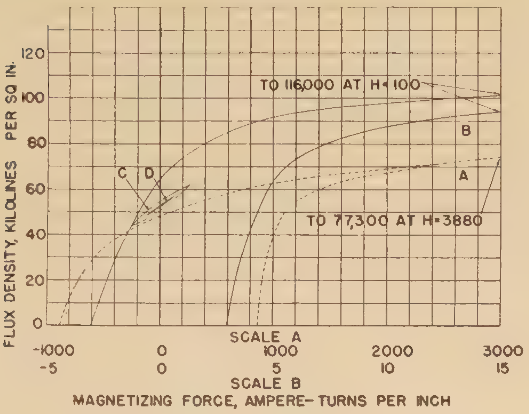


Figure 4 (left). Residual flux of permanent-magnet steel and ingot iron

A=Alnico permanent-magnet steel
B=Ingot iron, hydrogen-annealed
C=Minor loop in ingot iron
D=Minor minor loop in ingot iron

Nondestructive Test for Residual Flux

It was apparent at the outset that ballistic measurements of residual flux left by typical primary-current transients would not be feasible, since the zero-flux reference point would be obliterated by the transient; and also because splitting the core, even though the joint surfaces were ground, would introduce an unavoidable gap which would decrease the residual that was the most important quantity in the investigation. Also, it was felt that a method of residual-flux measurement using only portable meters and readily obtainable test power would be useful for field work. Another requirement for the investigation at hand was that the method should not decrease the residual flux very much in the process of measurement, since it was desired to produce a residual flux by applying a typical transient, measure this flux, and then use the flux after measurement to cause an error current which could be measured, or checked for its effect on a relay.

For the above reasons a method was devised which consists of applying to the secondary winding a very small measured 60-cycle magnetizing current and measuring the voltage necessary to circulate this current through the winding; this method will hereinafter be described as the impedance method of residual-flux measurement. By trial of this method it was found that the voltage decreased as the residual flux increased, and the relation between the volts per turn per square inch and the residual-flux density was determined experimentally by the use of a portable flux meter (which is equivalent to a ballistic galvanometer). This relation is shown in Figure 6 for a magnetomotive force of 0.258 root-mean-square ampere turns per inch of core length. The induced voltage is very sensitive to magnetomotive-force magnitude, as can be seen from the fact that a force half as great gave no measurable voltage, and so the curve should not be used to determine residual flux with any other value of magnetomotive force impressed. The voltages were measured by means of a rectifier type of voltmeter because of the relatively low impedance of the dynamometer type.

Table I. Characteristics of Current Transformers and Burden Used in Test

Core material	2.75 per cent silicon steel
Core construction	Spiral strip
Core treatment	Annealed after welding ends
Core diameters	$8^3/_4$ inches outside, $6^3/_4$ inches inside
Core height	$7^5/_8$ inches
Active core area	6.4 square inches (space factor 0.88)
Mean flux path	24.4 inches
Windings	Two, of 38 turns each, 0.12 ohm each
Burden at 5*A*	$0.65+j1.06=1.16$ ohms
Burden at 50*A*	0.72 ohm
Size constant (2)	491
Saturation curves	Figure 5

A determination of the decrease caused in residual flux by this magnetomotive force was made by leaving the flux meter connected during the instant when the measuring force was first applied; Figure 6 shows also this relation.

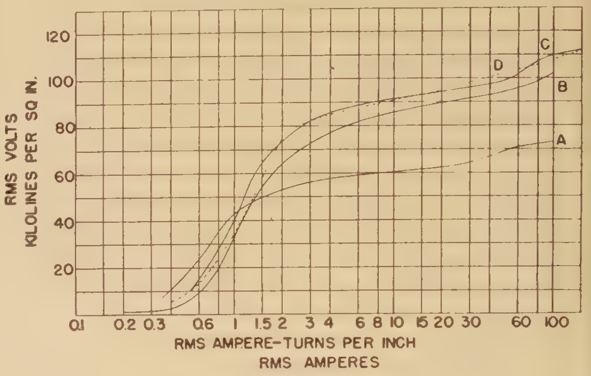


Figure 5. Saturation curves of current transformer and magnetization curves of the steel

A—A-c saturation curve, 38 secondary turns
B—D-c magnetization curve, 38 secondary turns
C—A-c magnetization curve of steel, calculated from *A*
D—A-c magnetization curve of steel, design data

Measured Values of Residual Flux

Measurements were made by the ballistic method to determine the relation between the flux density produced by application of a given value of magnetomotive force and the residual-flux density remaining after removal of this force. This test was made at various values of flux density up to saturation, and the results are shown in Figure 6. The results as plotted are a characteristic of the grade of iron in the transformer and not merely a characteristic of this particular size of transformer. It will be noted that the residual flux is 60 to 85 per cent of the maximum flux immediately preceding.

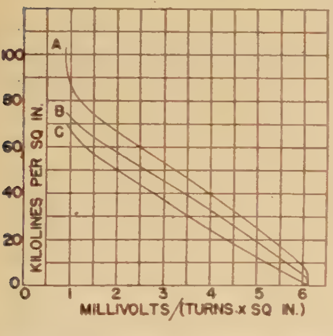
This information enables one to assign a maximum value to the residual-flux density which will be left in a current-transformer core by a given transient, if

Table II. Measured Flux Density Left in Current-Transformer Core by Various Transients

Trial	Oscillogram	Wave*	Amperes	Cycles	Flux Density, Kilolines Per Square Inch: Before Test	Added by Test (From Figure 3)	Residual (From Figure 6)	Residual Measured
1	8*A*	*S*	50	7.0	Small	40	24	39
2		*S*	50	6.5	0	162	75	53
3	8*B*	*S*	50	6.5	45	162	75	58
4		*S*	50	6.5	50	162	75	60
5		*S*	20	5.5	0	65	56	32
6		*S*	20	5.5	23	65	74	56
7		*S*	20	5.5	48	65	75	67
8		*S*	20	5.5	59	65	75	70
9	8*C*	*O*	50	6.0	0	380	75	35
10		*O*	50	6.0	27	380	75	67
11		*O*	50	6.0	60	380	75	67
12	8*D*	*O*	50	1/2	0	295	75	65

* *S* = Symmetrical.
O = Completely Offset.

he knows the initial density, core dimensions, turns, and voltage required by the burden for this particular transient; it is necessary only to determine from the usual transformer relation the peak flux density corresponding to the required voltage, add it to the initial density, and read from the curve the maximum residual-flux density that can be left in the core.



(Left) *A* = Maximum flux density during last half-cycle before applying test current
B = Residual flux density before applying test current
C = Residual flux density after applying test current

Figure 6. Relation between induced voltage and previous flux values, at 0.258 ampere turns per inch of core length, 60 cycles

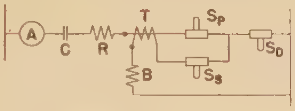
Measurements were made by the impedance method to determine the residual-flux density left by various transients such as are likely to be caused by lightning discharges or other insulation failure. To permit direct comparison of the results with various types of transients, the tests were all made at the same steady-state value of secondary current, approximately 50 amperes, with a burden of 1.16 ohms. The results are given in Table II. The measured values are usually less than the ones predicted from Figure 6, partly because the maximum density is less than the calculated value by reason of saturation of the burden and partly because the d-c component of error current partially restores the core flux to symmetry if the transient continues more than one cycle, thus further reducing the maximum density reached during the last half-cycle.

A test was made showing how residual flux can accumulate as a result of successive transients which happen to have the same polarity of initial half-cycle. The results are the second, third, and fourth trials listed in Table II; also the fifth to eighth, and the ninth to eleventh.

Permanence of Residual Flux

The core was magnetized and the residual-flux density, measured by the impedance method, was found to be 75,000 lines per square inch. The core was left undisturbed for about 40 hours, after which a check measurement gave the same readings.

A current of five amperes at 60 cycles was then applied to the primary, requiring a secondary voltage of 5.8 and a calculated peak-to-peak flux density of 18,000 and was left flowing for about 24 hours, after which another check measurement showed that the residual-flux density decreased to 23,000. Lower currents would cause smaller decreases, but no investigation was made of this relation, or of the relation between time and residual flux, at five amperes load.



A = Ammeter
B = Burden
C = Synchronous switch
R = Reactor with air gap
S_D, S_P, S_S = Shunts with oscillograph elements; difference current, primary current, and secondary current respectively
T = Current transformer under test

Figure 7. Circuit for oscillographic measurement of error current

Oscillograms of Error Current

The concept of error current or exciting current in a current transformer has sometimes seemed elusive, and it was thought that the nature of the phenomenon being investigated would be made clearer if we could get a direct measurement of this current in a form which would permit visualizing its relation to the primary and secondary currents.

An oscillogram provides the best means for visualizing an electrical quantity, especially a transient one, and so the apparatus was arranged to provide a portion of the circuit where error current could be measured directly. Referring to Figure 7, the primary and secondary windings, having an equal number of turns, were connected in series with oscillograph shunts. From the junction of these two shunts a third one was connected to receive the difference between the primary and secondary current; this difference is the error current, when the turn ratio is 1/1. When the current transformer transforms perfectly, there is no voltage drop across the error-current shunt and no current in it, but, when the ratio breaks down, the error current flows in this shunt. This method is subject to a slight inaccuracy, because, when this shunt carries current, there is a voltage drop across its terminals which helps the secondary induced-voltage drive current through the burden impedance, so that the error current obtained is somewhat less than it would be if the secondary circuit were closed directly on itself, and therefore the results are slightly optimistic. The maximum instantaneous drop across this shunt was 13 volts as compared with an instantaneous value (calculated from the oscillogram) at the same instant, of 23 volts induced in the secondary winding, but the inaccuracy of results seemed slight enough to be tolerated for the sake of obtaining a direct record of error current.

Typical oscillograms obtained with this circuit are reproduced in Figure 8. Oscillograms (*A*) and (*B*) show the difference in behavior, with symmetrical waves of primary current, depending on the absence or presence of residual flux; (*C*) and (*D*) show the corresponding comparison with fully offset waves. The residual-flux measurements show how the succeeding cycles of fault current in (*C*) removed a large part of the flux put in by the first half-cycle, which was similar to (*E*).

Effect on Relay

The final test of the hypothesis was an experiment to determine whether the increase in error current, already shown to result from the presence of residual flux in the core, would permit picking up a relay which would be restrained successfully in the absence of the residual core flux.

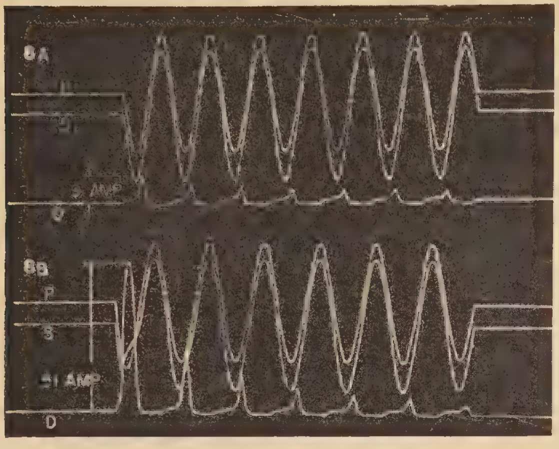
The burden for this test consisted of a

current-balance relay plus enough artificial burden to bring the total external burden up to $0.57+j0.82=1.01$ ohms. Including the internal resistance, the total resistance was 0.69 ohm, giving a size constant of 550.

In the course of this experiment it was found that

1. The normal transient error current without residual flux was sufficient to permit operation of a current-balance relay energized from the primary and secondary currents of the same 1/1 current transformer.

2. The difference between the transient error currents of similar current transformers without residual flux and with equal primary currents but with different burdens was sufficient to permit operation of a relay balancing the two secondary currents.



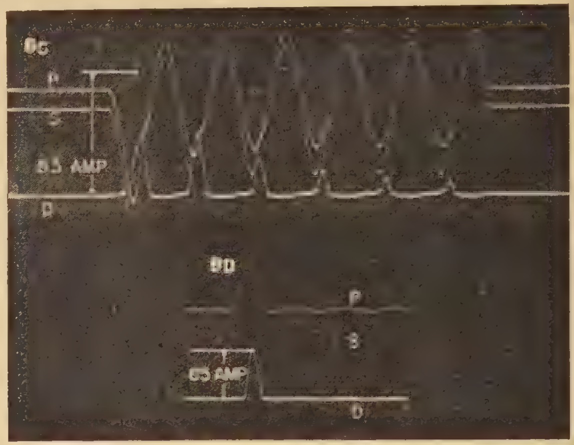


Figure 8. Typical oscillograms of error current (see Table II for identification)

D = Error current
P = Primary current
S = Secondary current

After these difficulties were overcome, it was found that

3. With similar current transformers, no residual flux, equal burdens, and equal primary currents, the relay would not operate.

4. With similar current transformers, equal burdens, and equal primary currents, but with residual flux in only one current transformer, the relay would operate.

The oscillograms of tests 3 and 4 are reproduced in Figure 9.

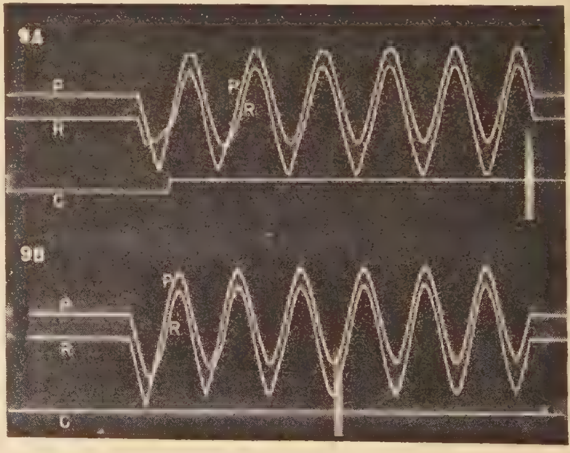


Figure 9. Oscillograms of relay operation with, and nonoperation without, residual flux

9A = With residual flux in only one current transformer
9B = Without residual flux in either current transformer
P = Primary current
R = Secondary current in restraining coil
C = Current in contact circuit

Effect on Generator Differential Protection

This phenomenon need have no effect on generator-differential protection using only two sets of current transformers, since they can be demagnetized before being put in service if it is suspected that they are magnetized, and their subsequent histories are identical. It should be considered, however if three sets of current transformers are used, as is sometimes necessary with double-bus arrangements or with station-auxiliary transformers tapped off between the generator and its circuit breaker. The reason is that the currents during a given fault, and therefore the residual fluxes resulting from it, are necessarily unequal in the three sets of current transformers; and a subsequent fault involving the opposite one of the two sets of current transformers in the line side of the generator winding will encounter different residual fluxes in the two sets involved this time and may cause false operation of the differential protection if its delay is inadequate.

Effect on Bus Differential Protection

This phenomenon affects differential-overcurrent relays used for bus protection just as much as it affects balanced-current relaying. However, as has previously been pointed out,[1] this effect is unimportant in comparison with core saturation caused by the d-c component of an offset current, and so it need be considered only in marginal cases, if at all.

Effect on Instantaneous Residual-Current Relays

Residual-current relays are often considered advantageous, because their settings are independent of load current. In some cases, such as a single line terminating in an ungrounded winding of a machine or a transformer bank, no selectivity is required, and therefore instantaneous relays have been used. Such relays are liable to false operation because of error currents resulting from different residual fluxes in the current transformers of different phases. Such inequality of residual fluxes in the event of a phase-to-phase fault can result from a previous ground fault, or from a previous phase-to-phase fault involving a different pair of phases. Such false operation of an instantaneous ground-overcurrent relay has been recorded in service, although it has not been determined whether the major cause was the residual core flux or the inherent difference in d-c component of the transient currents in the three phases.

Conclusions

While the phenomenon described is reproducible at will under controlled conditions, reported field experience indicates that the percentage of false operations that might be traced to it is small, and undoubtedly there are many locations where these improbable operations can be tolerated. However, some locations require the greatest obtainable reliability,

Effect of Radio Frequencies of a Power System on Radio-Receiving Systems

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IN the last few years considerable work has been done, in this country by the Joint Co-ordination Committee on Radio Reception of Edison Electric Institute, National Electrical Manufacturers' Association, and Radio Manufacturers' Association, and in Canada by the Department of Transport, to develop radio-noise measuring circuits and instruments.[1,2] The methods[2] of measuring radio noise and the instruments have reached the point in their development where the data obtained with these standard instruments can be correlated and the radio-influence characteristics of electric apparatus and power systems can be determined.

Radio co-ordination of electric apparatus is conveniently divided into two classes—low-voltage and high-voltage. The low-voltage class consists of apparatus used below 1,200 volts and frequently operated by and on the premises of the radio listener. Considerable co-ordination work has been done on this class of apparatus, and values of radio-influence voltage have been established for certain appliances and small motors. The high-voltage class includes apparatus used in this generation, transmission, and distribution of electric power. The problem of co-ordination of radio reception with this class of apparatus is more complex than it is for the low-voltage equipment and has not been completed.

This paper is concerned with high-voltage apparatus and describes tests and method for calculating the over-all effects on radio reception resulting from the radio-frequency voltages produced incidentally by power apparatus. The calculations depend upon a number of factors for some of which the range and probable values have not been determined. One of these factors for which information is required is the coupling factor of antenna to power lines and is defined as the ratio of field intensity at the radio listener's antenna to the field intensity under or near the transmission line. To determine this coupling factor, field tests were made at radio frequencies in a city receiving power from 25-kv transmission lines and 4-kv distribution circuits. A radio-frequency generator was connected to the 25-kv bus at the substation, and field-intensity measurements were made near the power lines and at 29 receiver antennas located at various distances from the 25-kv lines. The coupling factors to the nearest power circuit and to the 25-kv line were thus obtained for all the antennas tested.

Various other data useful in radio co-ordination were obtained. These include equivalent effective height of outdoor antennas used by radio listeners, types of grounds and distances from antenna to high-voltage and low-voltage lines.

Radio Co-ordination—High-Voltage Apparatus

In order to obtain a basis for evaluating the various factors in the radio co-ordination problem, it is necessary to provide a method of calculating the effect on radio reception of the radio-frequency voltages which may be generated by line and station apparatus. It can be shown that the following factors must be considered:

(*a*). Broadcast-station field intensity in microvolts per meter.

(*b*). Signal to noise ratio acceptable to radio listener.

(*c*). Ratio of interference field intensity at antenna to value at high-voltage line.

(*d*). Ratio of radio-influence voltage on line to field intensity under or near the line.

(*e*). Ratio of radio-influence voltage measured with standard laboratory-test circuit to voltage which will appear on line when apparatus is connected.

(*f*). Effect of service conditions.

Discussion of Co-ordination Factors

(*a*). Broadcast station field intensities depend on the locality and can be measured with the standard radio-noise meter. The magnitudes of the field intensities available as a function of the percentage of radio listeners is beyond the scope of this paper.

(*b*). The signal to noise ratio acceptable to radio listeners is probably in the range of 30 to 40 decibels. What the ratio should be depends on the type of radio program and the environment of the listener. Some background noise can be tolerated in most cases if the program is quite understandable without undue concentration provoked by the background noise.

(*c*). The coupling of antenna to high-voltage line determines the effect on the radio receiver of radio-frequency fields under or near the power circuits. Because the coupling factor varies over a wide range, it is necessary to use distribution curves for the data and to use values of the factor which are significant for the case or area considered. The tests described in this paper were outlined and made with the purpose of obtaining more information on this antenna-power-line coupling factor.

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and for them it seems necessary to seek a means of avoiding false tripping.

One means, but not a very practical one, is to demagnetize both sets of current transformers after each external fault when only one circuit is in service, and after each internal fault.

Another means is to use only relays with sufficient delay to permit the error current to reduce the residual flux and thus prevent completion of their false operation. This delay may be inherent, or it may be incorporated in an external definite time relay if the balanced-current relay has a dropout high enough to permit it to reset when the secondary currents again approach equality.

In cases where high-speed relaying is essential, the best solution lies in the use of current transformers with small or large air gaps in the cores.[2] Ironless couplers[3] could be used, but would provide a smaller output and require a more sensitive relay.

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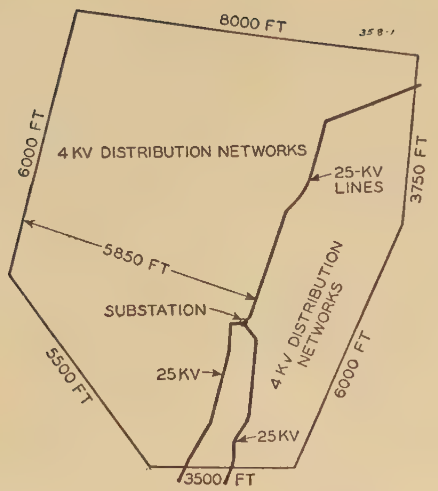

Figure 1. Outline of city in which radio co-ordination tests were made

Twenty-five-kilovolt lines to substation shown—four-kilovolt distribution spreads over area along with necessary street lighting, d-c feeder and telephone circuits

Twenty-five-kilovolt lines, four-kilovolt distribution, telephone on same poles along lines. D-c feeders also on two lower line sections

Table I. Antenna Characteristics

Type	Length (Feet)	Height (Feet)	Ground	Equivalent Effective Height (Meters)	Feet to Nearest High-Voltage Line	Feet to Low-Voltage Line	Coupling Ratio**
T	42x18	26	Rod	3.6	105	20	0.067
L	50	23	Gas pipe	3	50	12	0.3
L	30	14	Water pipe	0.4	45	21	0.55
L	40	21	Water pipe	3.0	114	10	0.5
L	75	28	Rod	5.0	63	8	0.45
Doublet	25x25	22	Water pipe	1.0	50	24	0.44
L	50	26	Rod	3.6	10	0.16	1.11
L	84	15	*	1.35	60	27	0.72
L	46	22		5.2	66		0.43
L	87	11	Water pipe	3.9	72	25	0.9
Doublets	30-30-12-12	25		0.78	130	8	0.18
L	60	18		13.9	78	25	0.6
L	100	28		1.0	66	20	0.8
Doublet	45x20	33		1.8	24	51	2.5
L	27	35	Water pipe	1.11	150	72	0.8
L	48	15	Water pipe	3.56	30	20	1.0
L	60	22	Water pipe	4.4	84	1	0.053
L	66	22	Water pipe	1.15	75	40	1.1
L	48	20	Water pipe	1.2	85	30	1.4
L	48	20	Water pipe	0.24	52	9	1.71
L (attic)			Water pipe	1.4	108	Home wiring	2.75
L	38	20		0.835	45	10	2.74
Doublet	51x20	29	Water pipe	3.86	75	19	0.21
L indoors					75	Home wiring	1.52
L	36	15	Rod		51	1	0.8
L	66	25	Water pipe	2.66	129	24	0.225
L					110	20	1.0
L					75		1.09
Loop					40		1.00
Averages	53 for *L*'s	22.3		2.83	73	20.7	0.93

* Where no ground was available, measurements were made by using neutral wire of 110-volt circuit as ground connection.

** This is ratio of field intensity at the antenna to the field intensity 30 feet from the nearest high-voltage line.

(*d*). This factor varies with the line height. Values from 55 to 90, average of 70, were measured 30 feet from the line, and under the line wires the values averaged only six per cent lower. In these cases there are 25-kv, 4-kv, and street-light circuits on the same poles, and the field under the line does not decrease with distance from the line so rapidly as it does with single-circuit lines.

(*e*). It is necessary to consider the effect of loading on radio-interference generators. Most generators connected to high-voltage circuits have several times the impedance (600 ohms) used in the standard high-voltage test circuit to load the apparatus. Because of the relatively high internal impedance the radio-frequency voltage which will exist when the apparatus is put in service on a transmission line is directly proportioned to the line impedance. For a line without reflections the load will be one-half the characteristic impedance which varies from 370 to 630 ohms, with an average of 494 ohms as obtained from measurements on several lines made at carrier-current and broadcast frequencies. The average is 494 ohms giving a value of 600/247=2.42 for (*e*).

(*f*). The effect of service conditions is not known. Electric apparatus is usually operated near its rating, and no large changes in the radio noise level will occur if ratings are not exceeded. In some cases the location, mounting, and proximity to ungrounded or grounded metal may be such as to change appreciably the electric field in the apparatus and outside of it in such a manner as to change the radio noise level.

The above factors can be expressed in the form of an equation:

$$RIV = \frac{ade}{bcf} \qquad (1)$$

where *RIV* is the radio-influence voltage referred to the standard high-voltage test circuit

If we use 31.6 (30 decibels) for b, 70 for

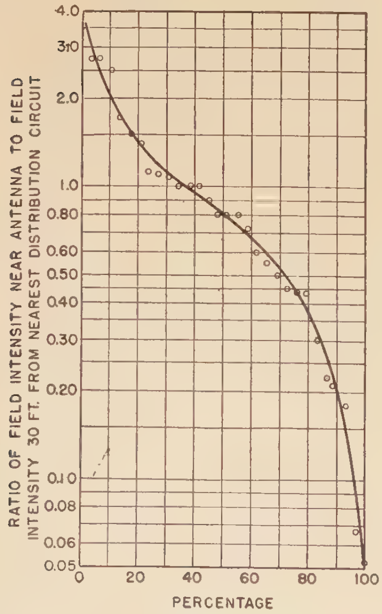

Figure 2. Percentage of total number of antennas tested with ratio (coupling factor) greater than ordinate

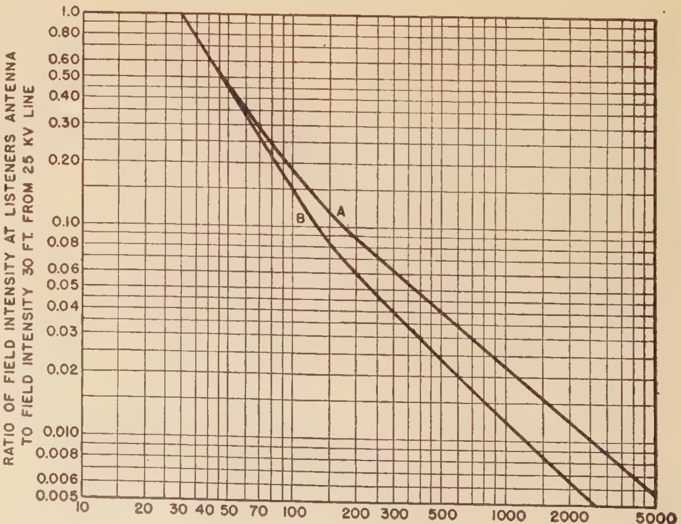

Figure 3. Coupling from high-voltage line to antenna as a function of distance from the line

(*A*). With distribution circuits over the area

(*B*). With no distribution circuits—all transmission through space

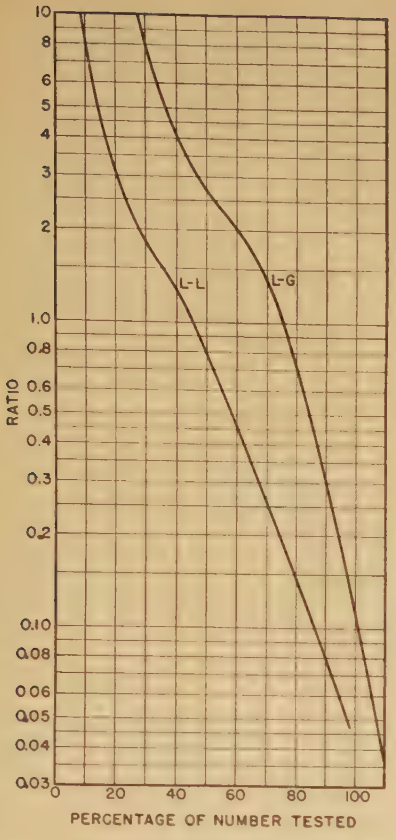

Figure 4. Ratio of line-to-line and line-to-ground voltages on 110-volt circuit to field intensity 30 feet from nearest high-voltage line

d, 2.42 for *e*, and 1 for *f*—all of which are reasonable values—and change nomenclature for broadcast field intensity from *a* to *E*, equation 1 may be written

$$RIV = \frac{5.36E}{c}\text{ microvolts} \qquad (2)$$

In this equation the factor *c* was considered as one requiring field investigation in order that numerical values of *RIV* for satisfactory radio reception could be obtained. Field tests were made therefore in an area as shown by Figure 1. This area has 16,000 people with 4-kv distribution, street-light circuits, street-railway network, and 25-kv lines located as shown.

Test Procedure

A slowly keyed signal of one frequency (980,000 cycles) was applied by means of capacitors to one phase of the 25-kv line at the substation. The location of this substation and 25-kv lines relative to the area inhabited by radio listeners is shown in Figure 1. The signal was of such a magnitude that it could be received, without interference, anywhere in the test region and along all transmission lines including the 132-kv lines feeding the 132-kv to 25-kv substation which is several miles from the test area.

Two standard[2] radio-noise meters and a standard signal generator were used as the measuring equipment. Test locations, that is, listeners' antennas, were selected at random along the 25-kv lines and at points several thousand feet and on both sides of the 25-kv lines. At each listener's antenna, the following 980-kilocycle (test signal) measurements or records were made:

1. Field intensity directly under and 30 feet from 25-kv or distribution lines whichever was nearest. (Thirty feet has been tentatively considered as a standard distance. In most cases it is not necessary to leave the roadway to take readings.)

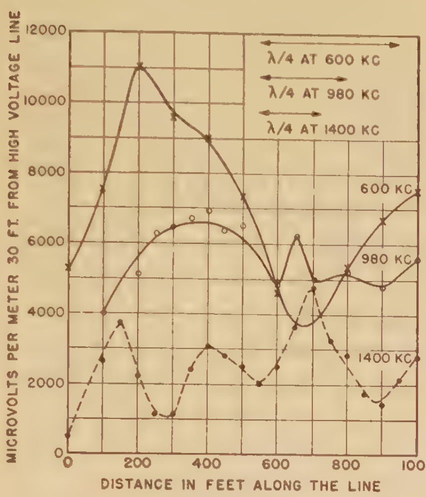

Figure 5. Variation in radio-influence field along same section of line at three frequencies

2. Field intensity at the antenna.

3. Voltage across listener's radio receiver with antenna connected.

4. Voltage across 1,000 ohms resistance—used in place of radio receiver. (This value of load resistance has been used by Department of Transport in Canada, as it makes possible comparison of antennas on a common load basis.)

5. Line-to-line and line-to-ground voltages were measured at the 110-volt 60-cycle outlet near listener's radio receiver. The network (Figure 6 of reference 2) was used for these measurements.

6. Distances from antennas to 25-kv line, to nearest distribution line, and to 110-volt line to house.

7. Several other measurements were made such as field intensity under 132-kv line connected to the same system as the 25-kv line to which test signal was impressed; readings of field intensity at three frequencies along the same section of 25-kv line; field intensity along and at the end of a short length of street light circuit.

Results of Tests

Antenna Characteristics

The data given in Table I are for 29 antennas—all of which are of reasonably good dimensions and good construction. It was found that 45 per cent used water pipe for ground, 28 per cent had no connections to ground, and the rest used ground rods which measured in some cases several hundred ohms with direct current. The average characteristics for the antennas tested are as follows:

Length: 53 feet for *L* types.
Height: 22.3 feet.
Equivalent effective height: 2.83 meters (this was determined by measuring the voltage across 1,000 ohms resistance connected from antenna to ground and the field intensity near the center of the antenna).
Distance to nearest high-voltage line: 73 feet (25 kv and 4 kv).
Distance to low-voltage line: 20.7 feet (110-volt circuits).
Average coupling ratio: 0.93 (ratio of field intensity at the antenna to the field intensity 30 feet from the nearest high-voltage line).

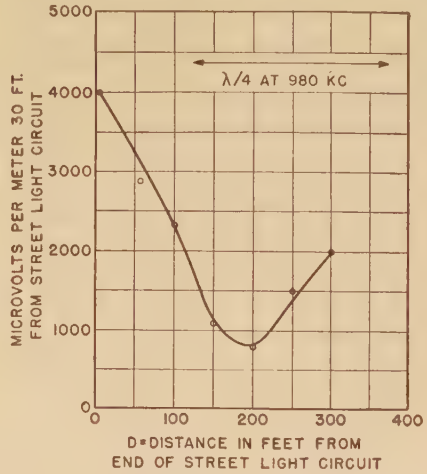

Figure 6. Radio-influence field increase at the end of a circuit

Coupling Ratio—Antenna to Nearest 25-Kv and 4-Kv Lines

The ratio of field intensity at the antenna to the field intensity 30 feet from the nearest power line is shown on Figure 2 as a percentage-distribution curve. These data indicate that the radio-influence fields at the antenna are on the average almost the same as the radio-influence field intensities at the nearest power circuit. There is no simple correlation between these coupling ratios and the distances to either high-voltage or low-voltage circuits.

Effect of Distribution Circuits on Transmission From 25-Kv Lines to Antenna

The field intensity was measured 30 feet from the 25-kv line at several points, and the average of these readings was compared to field intensities measured at the antennas. Curve *A* of Figure 3 shows the attenuation across an area having distribution circuits, and so forth. The loss is 0.0182 decibels average per foot taken from 100 feet to 1,000 feet from the 25-kv line. Curve *B* of Figure 3 is for an area without any other circuits in the vicinity and gives, therefore, the attenuation for space transmission in the vicinity of a transmission line. The attenuation in this case is 0.024 decibels average per foot from 100 to 1,000 feet. Curve *B* was extrapolated after 300 feet, assuming that the field intensity varies inversely as the distance. The effect of distribution circuits is to increase the field intensity up to about twice that obtainable by space transmission alone.

RADIO-FREQUENCY VOLTAGES ON THE 110-VOLT HOME CIRCUITS

The ratios of the line-to-line and line-to-ground voltages to the field intensity 30 feet from nearest high-voltage circuit are given as percentage distribution curves in Figure 4. (This ratio has the dimension of length with the unit being the meter.) The line-to-ground component is considerably greater than the line-to-line component. It was noted that measurements near antennas varied from home to home in a manner similar to variation of high-frequency voltages on home circuits.

COUPLING MEASURED BETWEEN CIRCUITS ON POWER SYSTEM

(*a*). From 25-kv line to 132-kv line through a station. In this case the two lines came to the station at about 90 degrees from each other, and the signal transfer was through the station. The field intensity measured under the 132-kv line was 18.5 per cent of that measured under the 25-kv line.

(*b*). From 25-kv line to 4-kv line and to telephone line. The four-wire four-kilovolt line is five to six feet away from and the telephone line 12 feet below the 25-kv line conductors. The voltage to ground on the four-kilovolt line was 8.2 per cent, and on the telephone line it was 2.4 per cent of the radio-frequency voltage on the 25-kv line.

VARIATION OF RADIO-FREQUENCY FIELD ALONG THE 25-KV LINE

The curves of Figure 5 show the variation in the field intensity which was obtained along a 1,000-foot section of 25-kv line. There is indication that the distance between maxima depends on the wave length. The difference between average and maximum values is about 50 per cent and is independent of the wave length. For the three frequencies the input voltage at the substation was the same, so that the curves represent the relative attenuation of signals over the network shown in Figure 1.

RADIO-FREQUENCY FIELD MEASURED AT THE END OF A CIRCUIT

It was observed during preliminary tests made with a receiver located in a car that the signal level increased whenever the end of a street-light circuit or of a four-kilovolt line was approached. In order to obtain data as to the change in the field, measurements were made along and to the end of one of these street-light circuits. The variation obtained in the field intensity is shown in Figure 6. There is a change from minimum to maximum of 5 to 1 and a quarter wave length occurs at approximately four-fifths the wave length corresponding to the velocity of light.

Conclusions

1. The radio-co-ordination problem has been resolved into the essential factors which can be determined independently. These factors can be combined to estimate the effect on radio reception of a given radio-frequency source, the characteristics of which have been measured by means of a standard test circuit.

2. Test data on the coupling factor *c* have been obtained, and it is believed that the distribution curve for the factor *c*, Figure 2, is representative for cities which have similar high-voltage and distribution lines.

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Application of Lightning Protective Devices in Wartime

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I. Introduction

UNDER present wartime conditions the need for conserving critical materials has been impressed upon the electrical industry with emphasis. It is for this reason that the lightning arrester subcommittee of the protective devices committee undertook a review of lightning protective devices and practices and their application to uncover and present any worth-while savings which it seems possible, practical, and reasonable to obtain. Within the short time since this project became active, it has not been possible to obtain committee agreement on all the various considerations involved. Therefore, rather than withhold from the industry the value of constructive study and suggestions already available, this paper is being presented without the sponsorship or any commitment by the lightning arrester subcommittee. It is hoped that its presentation and the discussions it will initiate will be helpful to the committee in continuing its work on this project to a conclusion. It is the purpose of the paper to give some results of a study on this problem up to the time the paper was prepared.

It should be pointed out clearly, however, that it is not the intent of this paper to imply that the suggested methods of protection and practices are suitable for universal application. Neither is it the intention to force the industry to use any of the suggested expedients where the particular conditions encountered are not suitable.

It is solely the intent of this paper to cover from a broad point of view factors in lightning-arrester protection which, if applied on an engineering basis, should result in some worth-while savings, both direct and indirect, in critical materials without lowering appreciably past standards of protection. In most cases, the protection should actually be improved.

II. Method of Approach

In undertaking this study, it was felt at first that the savings possible would be of limited extent. However, a further consideration indicated that, while an actual saving of critical metals was possible by applying lower-rated arresters where feasible, or by revamping old arresters, or reducing the length of connections and substituting other materials, there were extensive savings possible in the equipment to which the protective device was applied.

Carrying the analysis somewhat further, it was believed that a careful study of the protective problems of individual application would result in savings of both material and man hours for old installations already in service as well as for new installations being planned. At first thought, it might be assumed that the use of substitute materials for those now considered critical and the suggested reduction in arrester ratings in certain applications might introduce a greater hazard to the system as a whole. A more careful analysis of the situation, however, seemed to indicate that, in most cases, the over-all hazard to the system would be considerably reduced, although it might be expected that occasional failures of the protective devices themselves might result from the rebuilding of old equipment and the use of smaller arresters (line-type instead of station-type) and reduction of arrester rating.

The reports of some companies who had already put into use some of the methods discussed here indicated that satisfactory results were obtained. In many cases such changes from old practices had been made prior to the necessity for conserving scarce materials. The author, therefore, decided at this time to present and discuss the various proposed methods of conserving scarce materials and saving in man hours which are outlined below, since experience, limited in some cases, has been obtained already on some of the methods under service conditions. It is hoped that this paper will stimulate further interest and activity in the application of surge protective devices in a way that will save valuable critical material and man hours for the war effort.

III. Discussion of Proposed Methods of Protection

The work in recent years in getting a clearer picture of insulation strength of equipment and the protection afforded by protective devices has been presented and discussed quite thoroughly in the past and therefore will not be presented here. The lightning arrester and similar circuit-clearing devices have a very definite maximum voltage (60-cycle) rating which must not be exceeded in service without the possibility of failure of the device. In the past, many arresters have been applied to a system merely by matching the arrester rating with the system voltage without giving consideration to the actual voltage which might appear across the arrester in service. This is particularly true of solidly grounded neutral systems.

1. Reduced-Rated Arresters

The first consideration, therefore, is that of applying arresters on the basis of the highest system, or 60-cycle, voltage expected across their terminals in service. If this principle is adhered to, it is possible on many grounded neutral systems, at least, to use an arrester having a rating somewhat below, and in some cases considerably below, the line-to-line rating of the system. In the past, the entire field was covered by the general rule that on a grounded neutral system, the arrester would have a 60-cycle rating of approximately 80 per cent of line-to-line voltage. While this rule seemed to work out satisfactorily in most cases from an operating point of view, many cases exist today where an arrester of lower rating than one selected by this rule can be and has been used successfully. On a 132-kv nominal system, for example, the preferred rating for apparatus is 5 per cent higher or 138 kv, and the rating for arresters is 5 per cent above this value, or 144 kv. If the system neutral is grounded however, the industry recommendation for the arrester is 80 per cent of 144 or—the nearest standard arrester—121 kv. Thus, the use of the so-called grounded neutral arrester, instead of the full-rated arrester, on a system the neutral of which is grounded, results in a saving of approximately 20 per cent of the arrester material and cost, with considerably better protection provided. In carrying the analysis a step further, a calculation of the system overvoltage for each arrester application is made by our company, which showed in practically all cases that the overvoltage to ground does not exceed 109 kv. This results in a further

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decrease of 10 per cent in the materials and cost of the arrester. Three large companies have reported that they have been adopting this general practice of using the so-called reduced-rated arresters for grounded neutral systems for a considerable period of time with completely satisfactory results. The practice can be applied to both new installations and revamped old installations with direct savings of material in both cases.

This application of so-called "reduced-rating" arresters on solidly grounded neutral systems on the basis of the calculated 60-cycle system voltage under fault conditions instead of on the customary 80 per cent rule has resulted in savings other than in the arrester alone. It has been found possible in some cases to use equipment in the next lower voltage class, thereby using less critical material in the equipment itself.

2. LINE-TYPE IN PLACE OF STATION-TYPE ARRESTERS

A second consideration relates to the use of line-type arresters in the place of station-type in certain locations and under some conditions. Several savings are possible here:

1. In the amount of material required for the smaller line-type arresters.
2. In the possibility of less structural work for supporting the arrester.

It should be appreciated, of course, that the line-type arrester has a higher voltage level of protection than a station-type, and for this reason this application may not be desirable in some cases. On the other hand, where new equipment is involved, it is often possible to use the line-type arrester instead of the station-type and still obtain adequate protection. Four large users of arresters have reported the successful application of line-type arresters in place of station-type under some conditions.

3. PROTECTING EQUIPMENT NOT ALREADY PROTECTED

It might seem at first that the recommendation to add an arrester or similar protective device to equipment not now so protected is a "spending" of critical materials rather than a "saving." The broad point of view, however, is that the protection of equipment, subject to frequent hazards of overvoltage, may save that equipment from failure. It thus obviates the necessity of using critical materials for replacement of the damaged apparatus and further saves the man hours involved in building new replacement equipment or repairing the damage, as well as the man hours involved in replacement in the field.

It is not intended to imply that extensive and elaborate protective schemes be added to many pieces of equipment or in many situations where it is felt that past experience has given satisfactory performance, but a careful analysis of many situations in most companies will reveal cases where it is believed a thorough study and application of protective devices will yield considerable over-all benefits.

Cables, for example, subject to lightning require a large amount of critical material which well might be saved by suitable protection. Likewise, the protection of rotating equipment such as generators and large motors, subject to transient overvoltages, well may be given careful study, since the failure of such a piece of equipment may result in consequences more important than even the saving of critical materials. For example, the loss of generating capacity over a period of time or interruption to service to important war industry loads, although not "critical materials," is in a sense as important, if not more so.

4. REBUILDING AND REVAMPING OLD ARRESTERS

A fourth factor to be considered is the savings of materials made possible by rebuilding or revamping old-type arresters. Several companies have reported that they have been doing work along these lines for some time. In the case of oxide film arresters which were extensively used in the past, it is general practice in a number of companies to reduce the number of cells in existing arresters, which, in effect, means reducing the arrester rating to match more nearly overvoltage conditions of the circuit where they are used. The principle involved has been discussed in the preceding paragraphs. Likewise, the changing of four-leg oxide film arresters to the three-leg type has been an accepted practice in our own company for some time, with complete success from the viewpoint of added protection and absence of arrester failure. Of course, in such cases the 60-cycle maximum rating of the remaining three legs must be checked against the system overvoltage under fault conditions. In some cases where the three-leg 60 cycle rating was too low, part of the fourth leg was left in service to supply the deficiency.

Successful rebuilding of mica-spaced and open-gap autovalve arresters has also been reported. The rebuilding of old arresters supplies added protection to equipment, where they are installed by lowering the protecting level, and also may make available some of the material removed for other arrester locations or spare parts. The rebuilding of even obsolete arresters may be warranted in some cases, if new arresters are not available. The added protection of rebuilding obsolete arresters may be justified in some cases, although the protective level may be higher than would be desired ordinarily.

5. PROTECTIVE DEVICES OTHER THAN ARRESTERS

While the lightning arrester has been mentioned here quite extensively, it is recognized that there are other protective devices such as the protector tube, open-air gap, and control gap which also take their place in the matter of conserving materials. The decision as to which device to select is one of individual application and protection desired. It is believed that the characteristics of all of these devices are available or can be obtained with sufficient accuracy for purposes of application. All of these devices have been reported as having been used with satisfactory results. Whether to use one device in preference to the other in an attempt to save critical materials will probably depend in most cases on the immediate protective problem at hand and the availability of the device.

6. CONNECTING LEADS BETWEEN ARRESTER AND EQUIPMENT

A sixth consideration is the conducting material, usually copper, which can be saved by eliminating the ground leads from the ground end of arresters to ground. Where arresters such as station type or arresters mounted on the well-grounded structural steel of stations or steel poles are involved, the elimination in new designs or the removal in old designs of direct ground-wire leads can be accomplished without impairing the protective value of the arrester. The removal of such existing ground leads may not be highly practical, but in new installations it most certainly seems justified as a wartime expedient.

7. CRITICAL MATERIALS IN ARRESTER MANUFACTURE

Inherently, an arrester or other protective device of this type has a relatively small amount of critical material, that is, copper, iron, and aluminum. In one case at least, aluminum supporting castings have been replaced by galvanized iron. It has been suggested that the internal mechanism of the sealed gap element in arresters could be made in whole or in part of iron in the place of brass, but since both materials are so-called "critical," the change in arrester design would be predicated probably on which may become most critical and in the arc-interrupting characteristics of a design using iron. In the design of devices, therefore, it does not appear that there is room for any appreciable saving in critical metals, and probably the manufacturers have gone about as far as they reasonably can under present conditions.

8. PROXIMITY OF ARRESTER TO EQUIPMENT

An eighth consideration relates to the location of the arrester or protective device close to the equipment to be protected. This is now recognized as fundamental in applying overvoltage protective devices. By locating arresters this way and even relocating those now in service in the field, it is possible in some cases to save copper conductor at the ground and line leads and, at the same time, improve protection to the equipment. It is well known that under some conditions long leads in the arrester circuit permit the protective level at the equipment to rise appreciably above the level at the arrester terminals.

Cases where this proposal can be applied are at cable terminals where the arrester should be located directly across the pothead terminal rather than have a separate ground lead extending to ground, provided, of course, that the cable sheath is well grounded at the arrester location. Another case involves the location of distribution arresters one pole distant from isolated transformers or transformer banks. Such methods of attempted protection are considered of limited value in exposed locations where a long arrester ground lead is used and there is in addition a long line lead between the arrester and the transformer. In recent years the practice has been followed by several companies of locating arresters directly on equipment such as transformers, the transformer tank being provided with a supporting bracket on the tank. Arresters likewise have in some cases been mounted on the transformer covers. This suggestion can be applied both to new arrester installations and to many existing ones.

9. SHIELDING STATIONS

Another consideration applies to protection at stations where considerable electrical equipment is located and the equipment is built up of a large amount of critical material and will require replacement or repair in kind, as well as requiring a large amount of man hours and possible loss of factory output, if it should fail in service. The adequate protection of such stations seems almost a necessity. One way of greatly adding to the protection is by shielding the station as a whole, in addition to applying arrester protection. While shielding requires some critical materials such as steel, copper, or combinations thereof, it is suggested that careful consideration be given to such shielding in the light of relative importance of the equipment contained in the station. It is not recommended that all stations be shielded, but there are undoubtedly some which can initially use critical materials such as shielding wires with considerable, overall saving from a long range point of view.

10. TESTS AND MAINTENANCE OF ARRESTERS

The last subject considered here centers around the idea of keeping in the best possible operating condition equipment which we now have. This means, with the lightning arrester, a high degree of careful and planned maintenance. So far as the arrester is concerned, it is suggested that special consideration be given to testing arresters wherever possible and feasible, to insure that they are in the best of condition to perform their expected functions. Several years ago recommendations were made by the AIEE lightning arrester subcommittee on methods of testing some types of arresters. Since that time, a large number of arresters have been tested, and other methods of test or refinements have been used. It cannot be urged too strongly that a special attempt should be made to keep the arresters in the best possible condition.

IV. Summary of Benefits and Savings

That there are possible savings in critical material and man hours in the more careful study and application of protective devices already installed on the existing equipment and new applications seems evident from the preceding discussion.

A. The application of arresters on the basis of their rating, co-ordinated with the overvoltage of the system, will reduce to a slight degree the number of arrester units required on new work and make available some arrester units now in service for spares or new installations.

B. By closer application of arresters on the basis of their voltage rating, better protection will be provided to the protected equipment, and this should result in fewer equipment failures and, consequently, less need for critical materials and a saving in man hours used for renewal.

C. The use of lower-rated arresters also will result in lower insulation requirements of equipment such as transformers, for example, with consequent saving in steel in the containing tanks and core and some copper in bushing studs and winding.

D. The use of line-type arresters instead of station-type, where expedient, results in a direct savings of materials in the arresters themselves, as well as in possible savings in steel-mounting members and structures.

E. Applying protection on equipment not already protected can result in indirect savings in replacement of equipment which otherwise might become faulty if not protected.

F. Rebuilding of old arresters provides greater protection to existing equipment and eliminates the requirement of critical material for new modern arresters.

G. The omission and removal of ground leads from arresters already connected to well-grounded metal structures provide critical material for scrap or other uses.

H. A well-planned shielding system for exposed stations, although initially requiring critical material, may be more than compensated for by the prevention of equipment failure in such stations.

I. Testing of existing equipment to keep it in first-class operating condition likewise may reduce to a large extent the requirements for new equipment for replacement of protective devices and equipment which may become faulty in service.

It should be kept in mind that, in applying some of these suggestions, the failure rate on the protective devices themselves may increase, but it is believed that, if the conditions of each situation are considered carefully, and action taken on an engineering basis, the net over-all effect will be of considerable benefit to the war effort.

The Auto-Blast Interrupter Switch

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Synopsis: An air interrupter switch may be defined as a nonautomatic air switch which combines the functions of a disconnecting switch with the ability to interrupt current up to a predetermined magnitude. It primarily differs in function from a circuit breaker in that it cannot interrupt overload or short-circuit currents. General specifications for interrupter switches are proposed. The Auto-Blast interrupter switch is an air interrupter switch having each pole equipped with an arc chute into which the arc is driven by a blast of air generated automatically when the switch is opened. Interruption occurs within the chute by elongating and cooling the arc. The construction and operation of this switch is described, and test data are presented to show compliance with the proposed general specifications.

WHILE power circuit breakers are preferred for switching unit-substation and secondary-network transformers, it has been shown[1] that there is a definite economic field for metal-enclosed interrupter switches for these applications. The requirement for these switches exists because of the development of distribution systems having transformer capacity divided into a number of small banks and protected by a common-feeder circuit breaker. Economical switching provisions are necessary to provide means for disconnecting an individual transformer from a primary feeder without de-energizing the feeder.

Since an interrupter switch cannot open short-circuit current and a short circuit may occur at any time, there is always a certain degree of hazard associated with the operation of such switches. The probability of the occurrence of a short circuit in the transformer at the instant a switch is opened is remote, but the failure of a switch to interrupt the arc, for any reason, will probably result in a short circuit within the metal enclosure. Much can be done to minimize this possibility by giving due consideration to adequate design and tests. Good engineering judgment dictates the following general specifications for interrupter switches:

1. Long operating life with a minimum of maintenance.
2. Ability to interrupt current, within its interrupting rating, without arc or flame outside of the interrupting element.
3. A sufficient number of tests to demonstrate adequate safety factors in voltage, current, and operating life.
4. Speed of opening independent of operator.
5. Positive operation to prevent failure caused by partial closing and reopening.
6. An insulation level consistent with that of other apparatus in the circuit.

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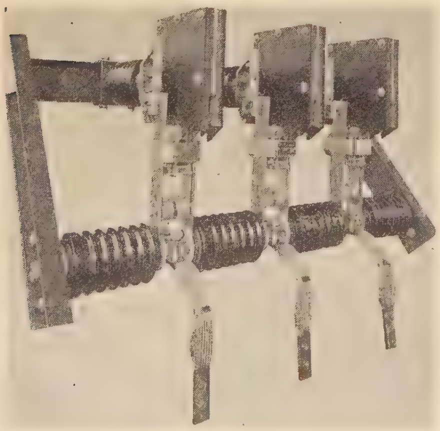


Figure 1. The Auto-Blast interrupter switch. 15 kv, 600 amperes continuous

Impulse insulation level 110 kv. Metal enclosure and operating handle removed

Table A. Three-Phase Reactance Load Interrupting Tests

Circuit Diagram Figure 6a

Tests	*Line-to-Line Recovery Voltage (Kilovolts)	**Test Current (RMS Amperes)	Arcing Time (Half-Cycles) Minimum	Arcing Time (Half-Cycles) Maximum	Arcing Time (Half-Cycles) Average	Muffler	Remarks
1 to 45	14.5	4–8	0.6	1.0	0.8	Off	No disturbance—cleared
46 to 49	15.0	4–6	0.7	1.0	0.9		
50 to 92	14.5	11–12	0.5	1.0	0.7		
93 to 96	15.0	11–13	0.2	2.0	0.9		
97 to 139	14.5	20–21	1.0	1.0	1.0		
140 to 144	15.0	20–23	0.5	1.5	1.0		
145 to 186	14.5	39–48	No oscillograms				
187 to 199	15.0	40–50	0.3	1.9	1.0		
200 to 243	14.5	88–125	No oscillograms			On	
244 to 248	15.0	91–129	0.3	2.0	1.3		
249 to 253	14.5	165–172	1.1	1.5	1.3		
254 to 255	15.0	171–178	0.9	2.8	1.5		

* Average three-phase voltage.

** Two values indicate minimum and maximum current interrupted for each series of tests.

Table B. Single-Phase Reactance Load Interrupting Tests

Reactance Load—Circuit Diagram Figure 6b

Tests	Line-to-Line Recovery Voltage (Kilovolts)	**Test Current (RMS Amperes)	Arcing Time (Half-Cycles) Minimum	Arcing Time (Half-Cycles) Maximum	Arcing Time (Half-Cycles) Average	Muffler	Remarks
256 to 258	5.2	69			1.1	Off	No disturbance—cleared
259	5.0	93			1.7		
260	5.1	113			1.1		
261	5.1	148			1.1		
262	4.9	165			1.0		
263	4.9	196			1.0		
264	4.6	220			1.4		
265	4.8	240			1.0		
266	7.5	98			1.1		
267	14.0	55			1.1		
268 to 270	14.0	54–55			1.0		
271 to 276	15.0	26–27	0.3	1.0	0.7		
277 to 282	15.0	56–58	0.8	1.1	0.9		
283	16.0	27			1.0		
284	16.0	39			0.8		
285	16.0	52			1.1		
286	17.0	29			1.0		
287 to 288	18.0	31			0.9		
289	19.0	32			0.8		
290 to 295	20.0	33–34	0.2	0.9	0.6		
296	22.0	36			1.0		
297	4.9	250			1.8	On	
298	4.7	270			1.8		
299	4.5	290			1.9		

** Two values indicate minimum and maximum currents interrupted for each series of tests.

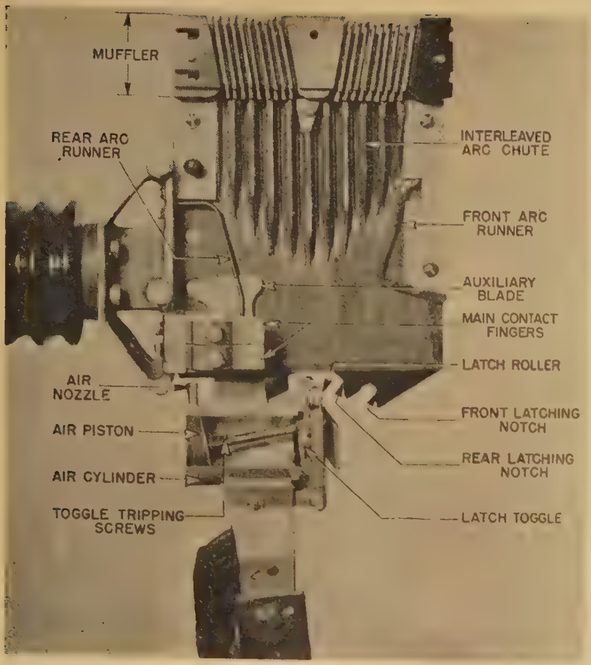
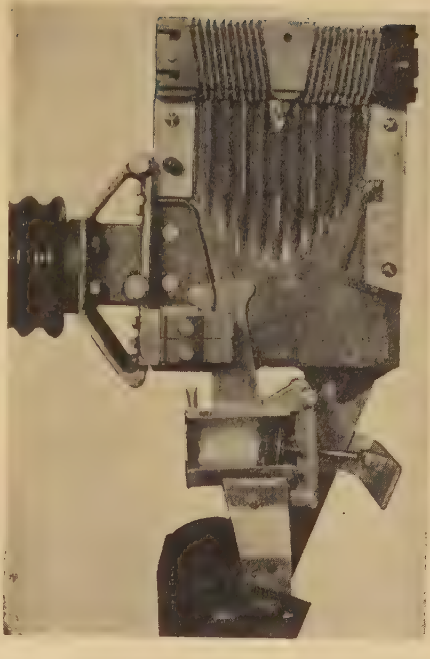
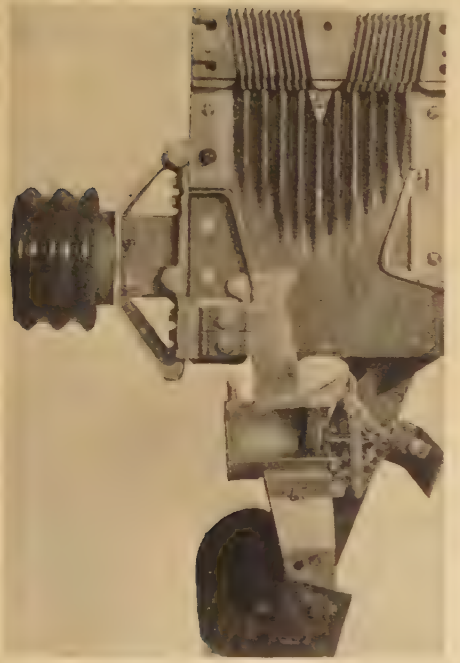


Figure 2. Cutaway views of Auto-Blast interrupter switch showing operation

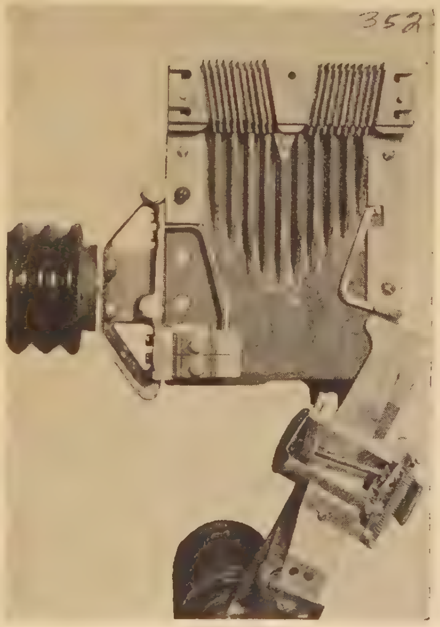
(a—left). Main and auxiliary blades closed and piston at end of exhaust stroke

(b—right). Opening operation. Main blade partially open, auxiliary blade closed and piston at end of intake stroke

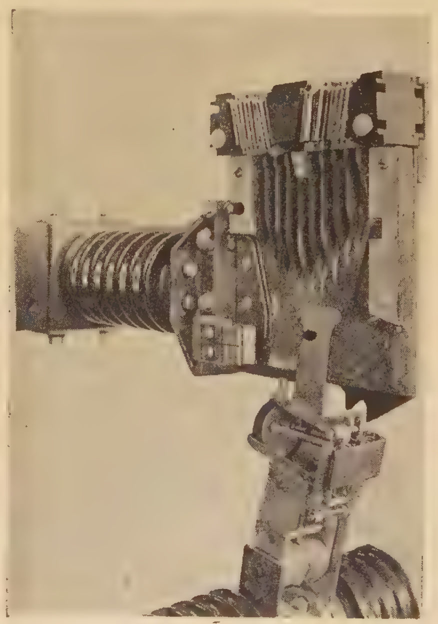




(c). Opening operation. Main blade partially open, auxiliary blade closed, with latch tripped and piston starting exhaust stroke



(d). Opening operation. Main and auxiliary blade partially open, latch reset and piston at end of exhaust stroke



(e). Closing operation. Main and auxiliary blade partially closed, toggle latch depressed

The Auto-Blast interrupter switch described in this paper is a new device which is intended primarily for application in a metal enclosure on the high-voltage side of unit substations and secondary-network transformers.

Construction and Operation

The Auto-Blast interrupter switch, Figure 1, is essentially a triple-pole group-operated device with three moving elements carried on a common rotatable operating shaft. The switch is arranged for mounting in a metal enclosure and for operating by means of a handle (not shown), keyed to either end of the rotatable shaft and located outside of the enclosure. In an opening operation the main switch blade opens first and, after a predetermined movement, trips a latch which releases the spring-actuated auxiliary blade. The auxiliary blade then snaps open, and the resultant arc is forced into the chute by a blast of air from a cylinder on the auxiliary blade. Figure 2 shows the construction of the switch and the relationship of the parts during successive steps of operation.

Each moving element of the switch has a pair of spaced main blades rigidly fastened to the operating-shaft insulators. An auxiliary blade and latch assembly is carried between and pivoted on the main blades at a point near the operating shaft. An air cylinder and nozzle form an integral part of the auxiliary blade, and a coacting air piston is attached to the main blades. A torsion spring normally maintains a parallel relationship between the main and auxiliary blades in which position the piston is at the exhaust end of its stroke.

Each stationary element of the switch consists of a contact and an arc chute assembly mounted on a fixed insulator. The contact assembly is made up of two spaced main contacts outside of the chute

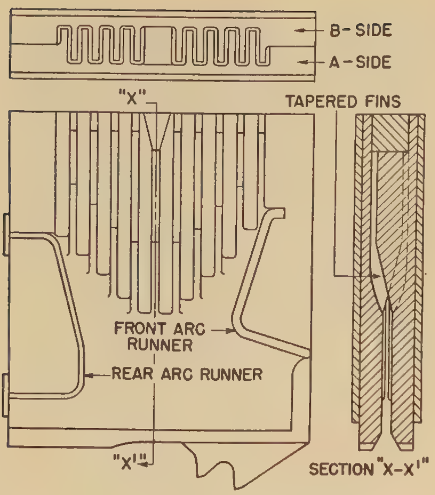


Figure 3. Interleaved arc chute

Side view with *A* side removed

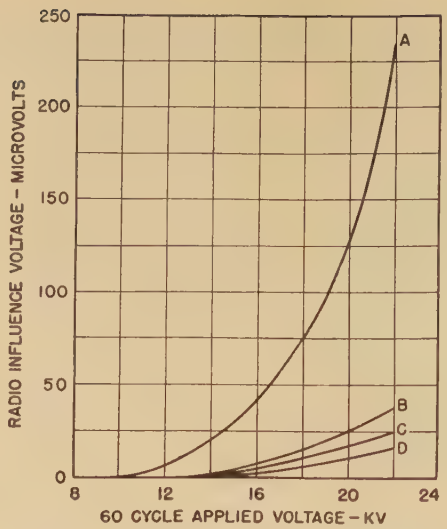


Figure 5. Radio-influence voltage tests on the Auto-Blast interrupter switch

	Impulse Level (Kilovolts)
A	60
B	75
C	95
D	110

and two arc runners centrally located inside the chute. The arc-chute assembly includes an interrupting element, with or without a muffler, and means for latching the auxiliary blade in the closed-circuit position during the first part of the opening stroke of the main blade.

In the closed position, Figure 2a, the main and auxiliary blades are in parallel, and the air piston is at the exhaust end of its exhaust stroke. When the operating handle is moved from the closed position, the main blade starts the opening travel, the auxiliary-blade latch engages the rear notch in the side plates of the chute assembly and holds the auxiliary blade in contact. As the main blade continues to move away from the fixed contact, the piston starts its intake stroke, and the auxiliary blade and air cylinder remain stationary until the switch reaches the position shown in Figure 2b. At this point the piston strikes and trips the toggle of the latch, Figure 2c, and the torsion spring snaps the auxiliary blade back toward the main blade and drives the cylinder over the piston forcing a blast of air from the nozzle into the arc chute. As the cylinder approaches the end of the exhaust stroke, the latch resets, the piston diaphragm covers the nozzle opening and traps air in the end of the cylinder, thereby cushioning the shock, Figure 2d. Both blades then move together to the full 90-degree open position.

In a normal closing operation, as the operating handle is moved from the open position toward the closed position, both blades and the toggle latch assembly move together until the latch is depressed, Figure 2e, by the cam surfaces formed on the arc-chute side plates. The latch roller then successively engages the front end and the rear latching notches as the switch is moved to the closed position, Figure 2a. The location of the front latching notch is such that it is engaged by the latch roller before an arc can be established during a closing stroke. This feature prevents the possibility of failure caused by partial closing and reopening.

The interleaved arc chute, Figure 3, is similar to the chute of the Magne-Blast air circuit breaker.[2] It is formed by tapered fins alternating from either arc chute wall. The material of the chute is treated especially to be arc resistant and essentially nongas producing and nonexpendable. The entire arc-chute assembly is easily removable for inspection of contacts, without disturbing the adjustment of either the chute or the switch parts.

The arc is drawn in a narrow slot between the auxiliary-blade tip and the rear arc runner. As it is forced up the runners and into the chute, it progressively lengthens and encounters an increasing cooling effect as its path becomes longer. The maximum current that can be interrupted with no disturbance external to the interrupting element is a function of the size of the arc chute and of the pressure and volume of the air blast. When interrupting relatively low currents, such as transformer exciting current, the arc and incandescent gases are entirely confined within the short simple chute of Figure 3. With increased current, as indicated in the accompanying tables, the air blast tends to drive the incandescent

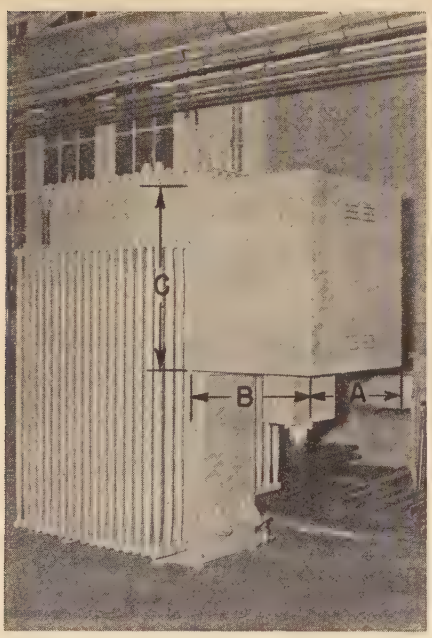
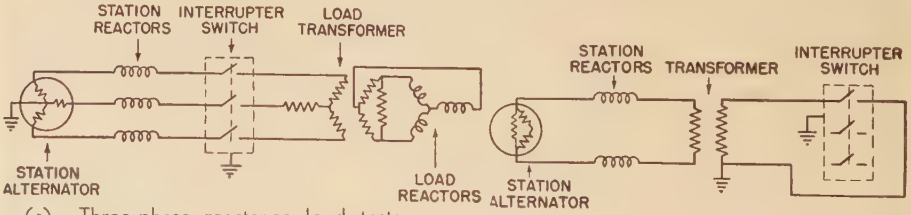


Figure 4. Dimensions of metal-enclosed interrupter switch mounted on transformer

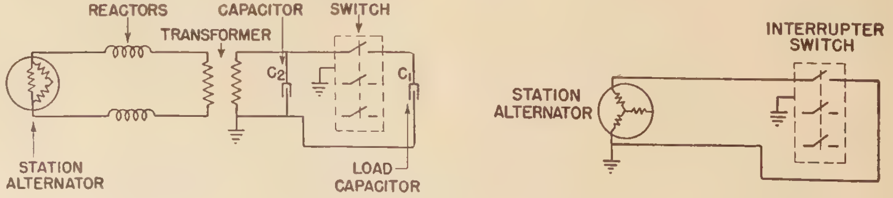
Impulse Level (Kilovolts)	A Width (Inches)	B Depth (Inches)	C Height (Inches)
60	28	27	43
75	35	31	43
95	38	32	44
110	43	34	45

Figure 6. Test circuit diagrams



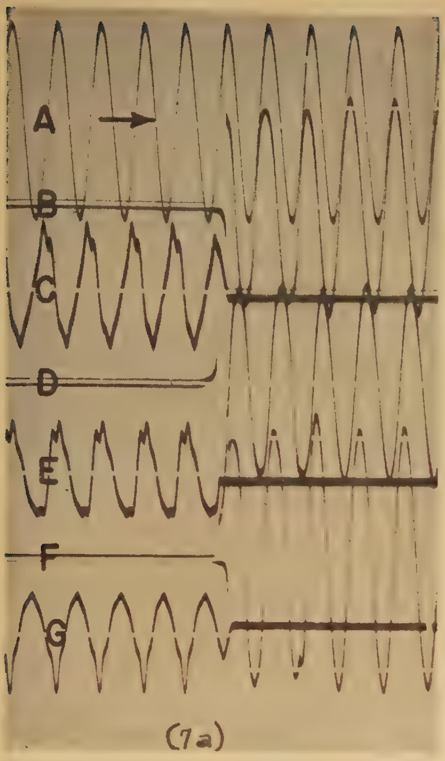
(a). Three-phase reactance load tests

(b). Single-phase reactance load tests

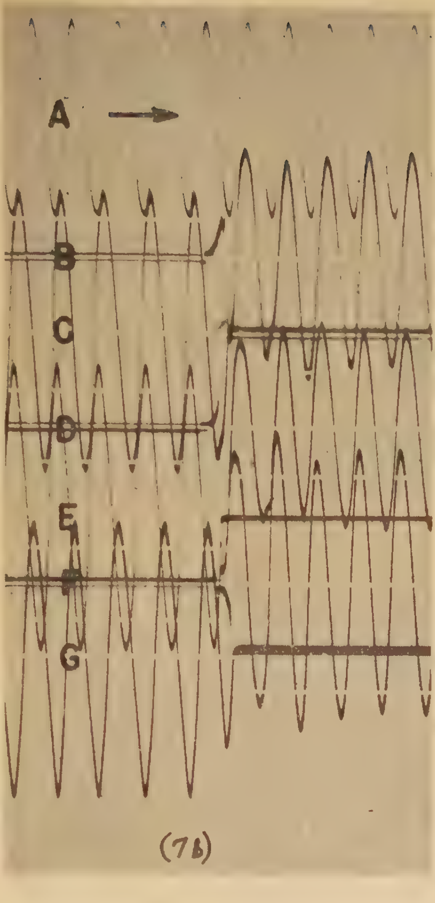


(c). Single-phase capacitance load tests

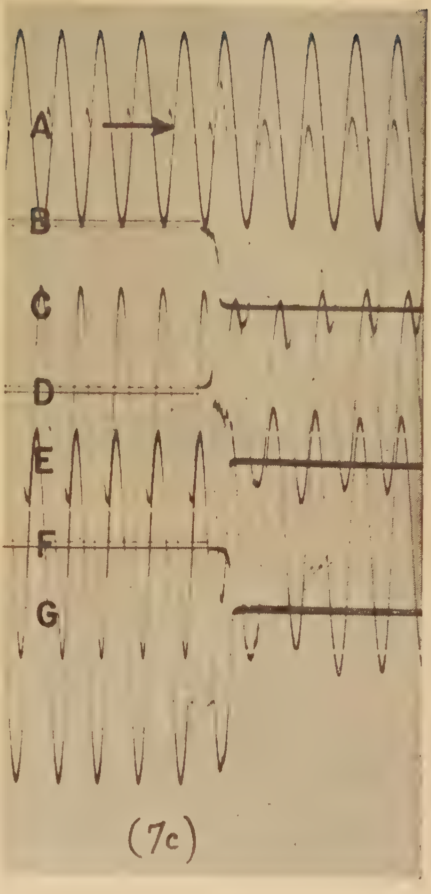
(d). Single-phase short-circuit tests



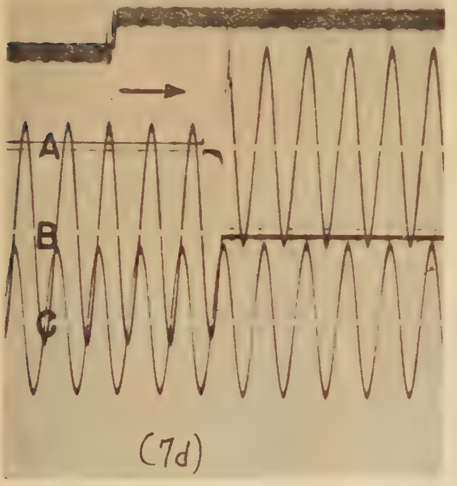
(a). Test 47
A—Timing wave
B—Voltage across switch—line 1
C—Current through switch—line 1
D—Voltage across switch—line 2
E—Current through switch—line 2
F—Voltage across switch—line 3
G—Current through switch—line 3



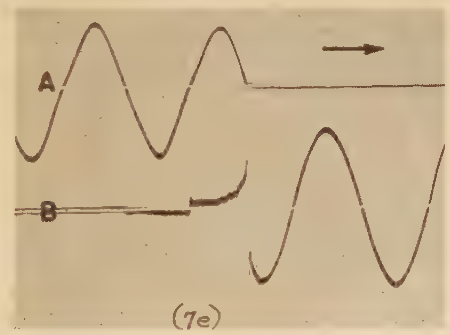
(b). Test 189
A to G same as (a)



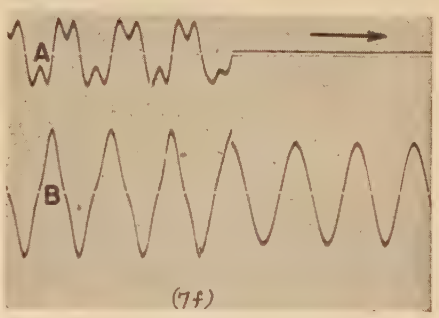
(c). Test 254
A to G same as (a)



(d). Test 296
A—Voltage across switch
B—Current through switch
C—Timing wave



(e). Test 299
A—Current through switch
B—Voltage across switch



(f). Test 322
A—Current through switch
B—Voltage across switch and capacitor

Figure 7. Typical oscillograms

Refer to Tables A, B, and C for data

arc gases through the short chute before adequate cooling can take place. The interruption of relatively high currents can be successfully accomplished in the same arc chute by the addition of a suitable muffler to cool further the arc gases.

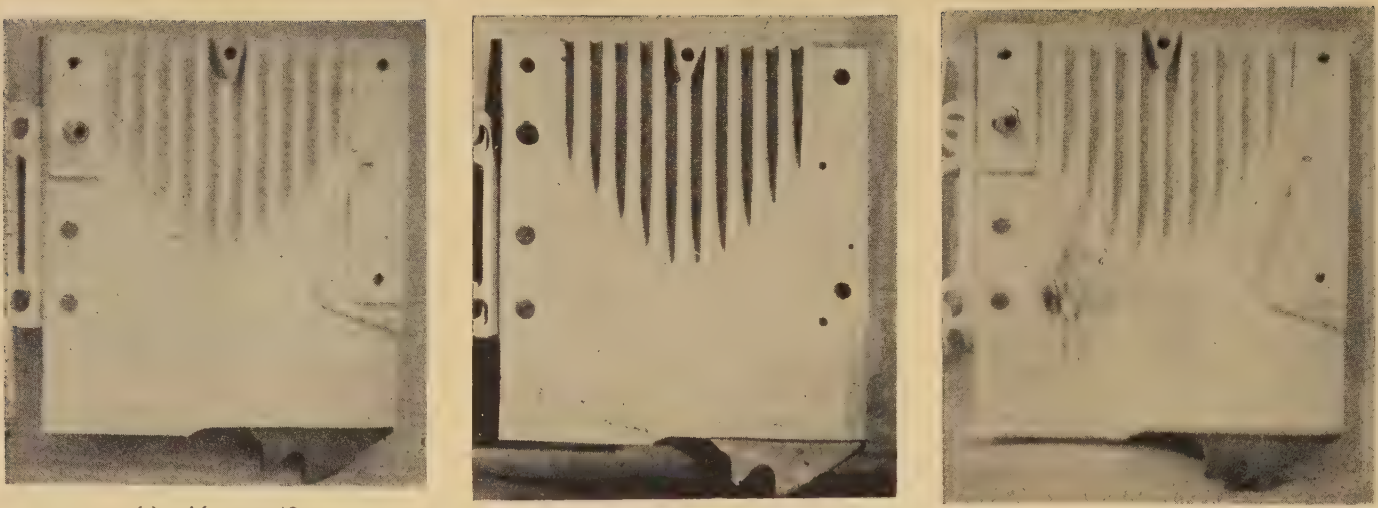
The combination of a small basic arc chute for low current and an easily attachable muffler for higher current provides the most economical arrangement for handling the complete range of current under consideration. It further provides a convenient means of increasing the interrupting ability of existing devices originally intended for the interruption of transformer exciting current only.

Since space is always at a premium, it is important that the dimensions of the switch and its enclosure be maintained at the minimum commensurate with adequate insulation levels. The required impulse-withstand test voltage is the major factor in determining the over-all dimensions of the Auto-Blast interrupter-switch enclosure shown in Figure 4. Dimensions of such magnitude must, of necessity, accompany the degree of insulation obtained.

Tests

The Auto-Blast interrupter switch, mounted in a metal enclosure, has been tested thoroughly to check its conformance with the design specifications.

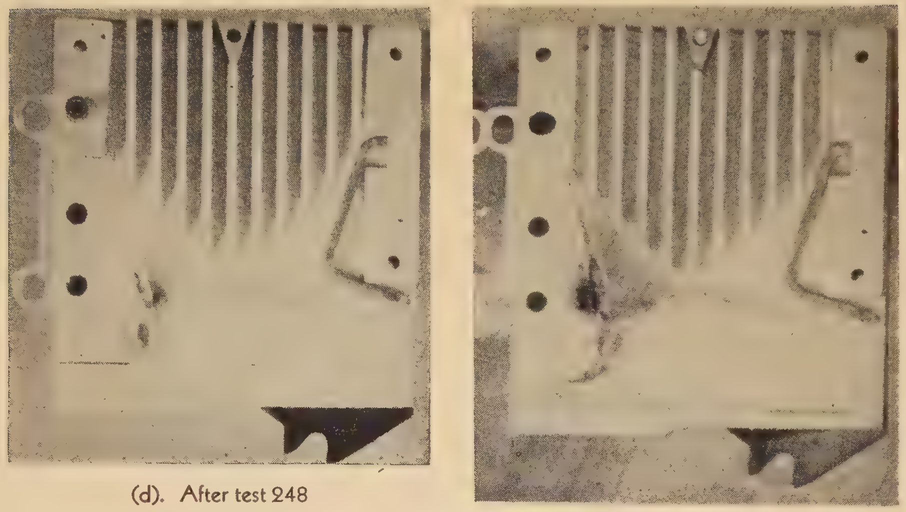
Radio-influence tests in both the open and closed positions of the switch with the metal enclosure grounded, Figure 5, show that the radio-influence levels of the interrupter switch for all classes of insula-



(a). After test 49

(b). After test 144

(c). After test 199



(d). After test 248

(e). After test 255

Figure 8. Arc chutes after three-phase interrupting tests

tion meet the limits established in the National Electrical Manufacturers Association Standards.[3]

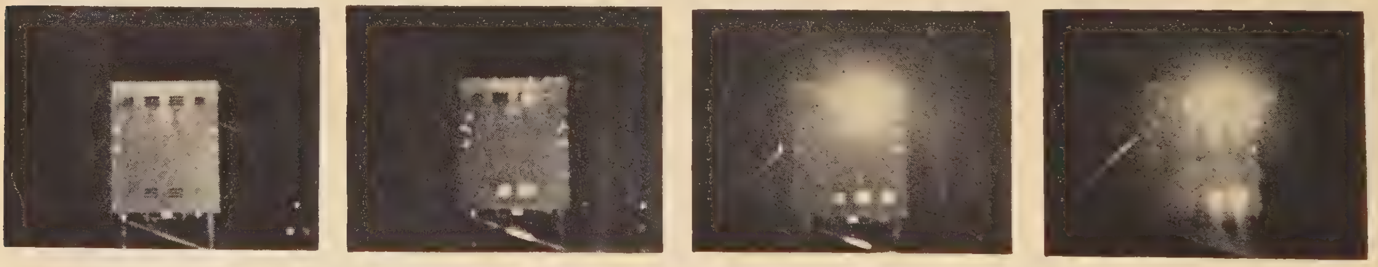
Dielectric tests, both 60-cycle and impulse, made on the switch in the open and in the closed positions with the metal enclosure grounded, determined the insulation required and the sizes of the metal enclosures, Figure 4. These tests were made in accordance with the AIEE Standard[4] for air switches.

Interrupting tests were conducted over a two-year period to investigate a number of basic methods of interrupting transformer exciting current and load current. More than 5,000 tests showed the Auto-Blast design to be by far the most outstanding. A few of the results are shown in Tables A, B, and C with typical magnetic oscillograms in Figure 7.

Various circuits were used to cover the complete range of current and voltage. To simulate in test the most severe service conditions, a value of 20 per cent power factor was established as the maximum at which interrupting tests should be made. However, all of the reactance-

Figure 9 (below). Test 326

Attempting to open short circuit of 30,000 amperes initial, 16,400 amperes final at 14.5 kv. Arc duration 23 cycles

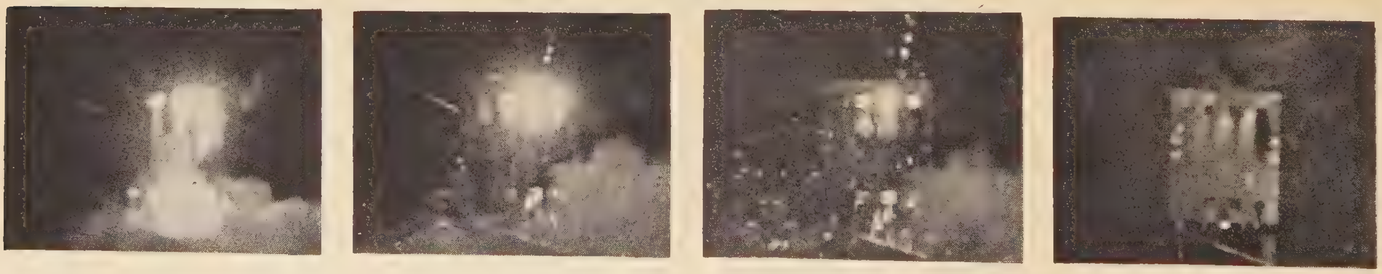


(a). Before test

(b). Switch starts to open

(c). After one cycle of arc

(d). After four cycles of arc



(e). After nine cycles of arc

(f). After 17 cycles of arc

(g). After 19 cycles of arc

(h). After test

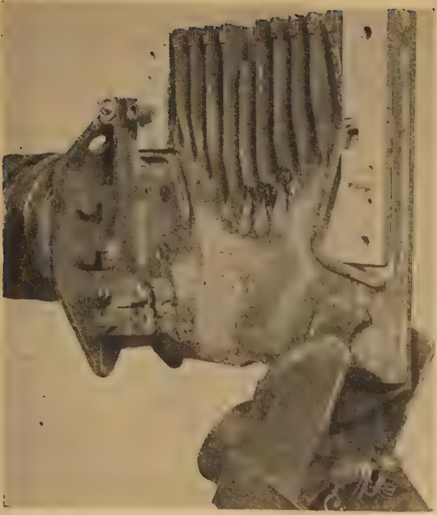


Figure 10. Switch parts after test 326

Table C. Single-Phase Capacitance Load Interrupting Tests

Single-Phase Capacitance Load Tests—Circuit Diagram Figure 6c

Tests	Line-to-Line Recovery Voltage (Kilovolts)	**Test Current (RMS Amperes)	Line-to-Ground Capacitance (Microfarads) C_1	Line-to-Ground Capacitance (Microfarads) C_2	Muffler	Remarks
300 to 307	15.0	26–33	4.4	None	Off	No disturbance—cleared
308 to 311	15.0	29	4.4	4.5		
312 to 314	15.0	57	8.8	None		
315	15.0	57	8.8	8.4		
316 to 320	15.0	57	8.8	8.4	On	
321 to 324	15.0	115–120	17.6	Nonz		

** Two values indicate minimum and maximum currents interrupted for each series of tests.

load interrupting tests were made with the current limited almost entirely by reactance, and the power factor in all cases approached zero.

The entire series of three-phase reactance-load interrupting tests in Table A was made on one set of arc chutes and without maintenance of any kind on the switch or chutes. After each group of tests, the switch was examined, the arc chutes removed, dismantled, and photographed. The only evidence of change during the entire series of tests was a slight pitting of the arc runners and a discoloration of the arc chutes by a nonconducting oxide deposit, Figure 8. After tests the chutes were in excellent condition and were obviously suitable for at least 1,000 successful operations.

The single-phase reactance-load interrupting tests, Table B, demonstrate that the 15-kv switch, without a muffler, performs successfully at voltages up to 22 kv across a single pole to ground. The single-phase capacitance-load interrupting tests in Table C, made with and without capacitance on the line side of the switch,[5] indicate the possibility of switching capacitor banks with the Auto-Blast interrupter switch.

Short-circuit tests at 14.5 kv, Table D, were made with the switch in the circuit near the generator terminals. The switch with manual mechanism was closed with a fast positive operation against a fault current of 31,000 amperes. The switch parts were damaged, but all resulting disturbance was confined within the metal enclosure. An opening test under approximately the same short-circuit current conditions produced a very severe disturbance as shown by high-speed motion pictures, selected frames of which are reproduced as Figure 9. Flame, hot metal, and gas were blown through the ventilating louvres and through the joint between the cover and the housing. The switch parts were badly damaged as shown in Figure 10, and most parts would have required replacement before the switch could have been put back in service.

Table D. Single-Phase Short-Circuit Tests—Circuit Diagram Figure 6d

Test Number	Line-to-Line Test Voltage (Kilovolts)	Test Current (RMS Amperes)	Operation	Remarks
325	14.5	31,000	Close	No disturbance outside of housing except slight trace of smoke from louvres
326	14.5	30,000 initial 16,400 final	Open	Loud report with much smoke, flame, and hot metal from housing. Several bolts from lower edge of cover blown off

Conclusions

1. Interrupter switches in general should be liberally designed and rigorously tested, since any failure in normal operation probably will result in a short circuit.

2. The Auto-Blast interrupter switch fully meets all modern requirements for high insulation level, low radio-influence voltage, and adequate safety factor in interrupting ability for the service intended.

3. The interrupting element of the switch is arc resisting, essentially nongas producing and nonexpendable, thereby providing long operating life with minimum maintenance.

4. The performance of the switch is consistently uniform with an arcing time in the order of one to two half-cycles at all currents within its interrupting rating.

5. It has been demonstrated that the switch, when closed with a fast positive operation, has mechanical and thermal strength to withstand closing against short-circuit current not exceeding the short-time current rating of the switch with disturbance and damage confined within the metal enclosure.

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5. TESTS AND ANALYSIS OF CIRCUIT-BREAKER PERFORMANCE WHEN SWITCHING LARGE CAPACITOR BANKS, T. W. Schroeder, E. W. Boehne, J. W. Butler. AIEE TRANSACTIONS, volume 61, 1942, November section, pages 821–31.

Harmonics and Load Balance of Multiphase Rectifiers

Considerations in the Selection of the Number of Rectifier Phases

R. D. EVANS
FELLOW AIEE

Synopsis: This paper analyzes the operating characteristics of multiphase power rectifiers from the standpoint of harmonics, load balance, and their interrelation, and their effects on the apparatus and circuits to which they may be connected. These factors are considered in connection with selection of the number of rectifier phases, which varies from six (four for very small sizes) to combinations of six-phase units to form a much larger number, such as 36, 72, or even 108 phases. More specifically, the problems of harmonics, load balance, and number of phases are considered from the standpoint of the effects on

1. Apparatus, particularly turbine generators and capacitors.

2. The wave-shape distortion on circuits that may be important in inductive co-ordination.

3. The possibility of resonance being encountered in the supply system to amplify one or more of the rectifier harmonics.

These problems have increased greatly because of the great number and large size of rectifier installations made in connection with the war.

Determination of Harmonics in the A-C Supply Circuits

It is a well-known property of rectifiers and inverters that they produce distortion of both voltage and current waves which appear on both the d-c load circuits and a-c supply circuits, even though the latter contains no generated harmonics. The important harmonics of rectifier installations for all except traction application are restricted normally to those in the a-c supply and are the only ones here considered.

The harmonics in the supply system resulting from the operation of balanced rectifiers or inverters include the series $(pn+1)$ and $(pn-1)$, where p is the number of rectifier phases and n is a series of integers. For power applications the number of rectifier phases is usually limited to six or multiples thereof. Thus, the harmonics of a six-phase rectifier are the 5th, 7th, 11th, 13th, 17th, 19th, and so forth; the harmonics of a 36-phase rectifier are the 35th, 37th, 71st, 73d, and so forth. Actually, rectifier circuits are not perfectly symmetrical or balanced so that some harmonics, not characteristic of the total number of rectifier phases, may be present in reduced magnitude. Thus, if a rectifier of 36 phases is operating with one six-phase unit out of service, the 5th, 7th, 11th, 13th, 17th, 19th, and so forth, harmonics will appear in the supply as well as the 35th, 37th, and so forth, harmonics which are characteristic of a balanced 36-phase installation.

Methods are available for the calculation of harmonics in the supply circuit to a rectifier.[1–3] The theoretical methods[2,3] are based on Fourier analyses, assuming that the inductance of the d-c load circuit is very high and that the impedance of the supply circuit has a linear frequency-reactance characteristic. One method[3] takes into account the loading of the rectifier, the reactance of the supply, and the amount of voltage reduction by phase control. This method gives excellent results when the assumptions upon which it is based are approached. Extensions of this method are also described[3] for application to cases where the supply circuit does not have a linear frequency-reactance characteristic. These extensions are based on finding an equivalent 60-cycle supply reactance corresponding to the actual harmonic reactance of the supply.

Heating of Generators

Generators supplying rectifiers are subjected to increased heating because of harmonics, particularly local heating in rotors. The heating problem is more important for turbine generators than for salient-pole machines, being particularly less for machines provided with damper windings. The importance of this problem arises principally in connection with generators that are loaded fully with six-phase rectifiers and is of much less importance with 12-phase rectifiers. Conversely, the problem is of negligible importance for rectifiers of more than 12 phases and for conditions where the rectifiers are a small part of the total load. A favorable circumstance in the generator heating problem arises from the fact that usually the rectifier load is of higher power factor, 90 to 95 per cent, than generator rating, 80 per cent power factor. This condition reduces excitation loss and thus provides a margin for increased loss from harmonic currents.

Heating of Capacitors

Commercial capacitors are designed to carry in addition to the rated kilovolt-amperes at fundamental frequency, 35 per cent additional kilovolt-amperes to provide for overvoltage at fundamental frequency or for harmonics. The total kilovolt-amperes of 135 per cent of rating may be made up of fundamental and harmonic kilovolt-amperes added arithmetically. Thus, a capacitor will withstand 105 per cent voltage of fundamental frequency and 112 per cent current of fifth-harmonic frequency. The designs further assume that the average fundamental-frequency voltage will not exceed 105 per cent normal and that the short-time magnitude will not exceed 110 per cent. If capacitors are applied correctly with respect to fundamental-frequency voltages, the heating limitations will rarely be encountered unless the supply is subjected to very abnormal harmonic conditions which arise principally as a result of resonant conditions.

Wave Shape

Harmonics in supply circuits which parallel communication circuits may give rise to important noise-frequency co-ordination problems. These problems have been studied extensively by the Joint Co-ordination Committees of the Edison Electric Institute and the Bell Telephone System. One result of this work has been to set up two factors which take into account the over-all influence characteristic of a power-supply circuit from the standpoint of its tendency to produce noise in an adjacent telephone circuit. Because of the different effects of electric (electrostatic) and

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magnetic induction, separate factors are required, one for current and the other for voltage. These factors as applied to balanced circuits supplying rectifiers are the balanced $I \cdot T$ and $KV \cdot T$ factors; where I represents the rms balanced current and KV, the rms voltage in kilovolts between line terminals of the three-phase circuit, and T, condensed from *TIF* (telephone influence factor), is a factor which gives the relative weighting of the various harmonic frequencies of a power system from the standpoint of their tendency to produce noise in an adjacent telephone circuit. Two different frequency-weighting curves are commonly used in rectifier studies, namely, the 1935[4] and the 1941[5] frequency-weighting curves.* The difference in weighting results from different characteristics of the telephone circuit including terminal equipment. The principal change in the 1941 weighting from the 1935 weighting results from the substantial elimination in the later telephone receivers of a resonant point in the vicinity of 1100 cycles. At the present time both types of telephone receivers are in service and both types of frequency-weighting factors are used in co-ordination studies. While the 1941 frequency-weighting curve is expected to supersede the 1935 curve, time is required for the transition, and this has been delayed further because of the war. The shape of the 1941 frequency-weighting curve has been established, but its arbitrary scale has been delayed in order to determine for it a numerical value of the same importance as the 1935 curve. For present purposes the comparisons, based on the relative shapes of these frequency-weighting curves, are sufficient.

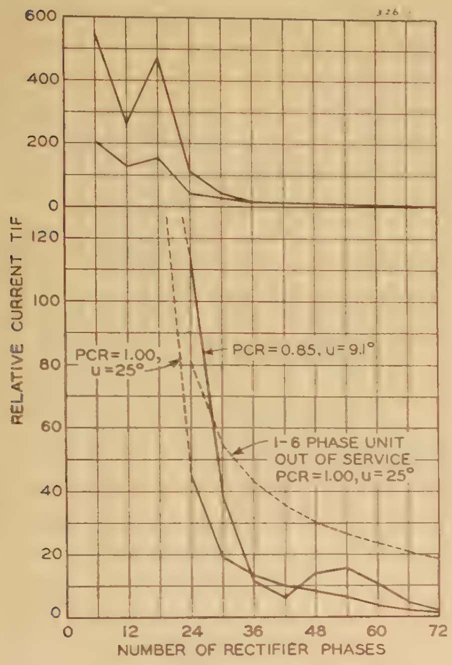

Figure 1. Effect of number of rectifier phases on relative current *TIF*

Based on 60-cycle supply and 1935 frequency-weighting curve

Actual current *TIF*=(relative current *TIF*)K' where

$$K' = \frac{I}{RI_{ac}}$$

I = D-c load current per secondary phase group

I_{ac} = Alternating current on primary side of transformer, rms value

R = Transformer ratio, primary to secondary line-to-neutral voltages

Insert: Replot of principal curves to smaller scale

An investigation, which is reported in the present paper, was undertaken to determine the relative influence of the number of rectifier phases on the inductive co-ordination problem, particularly for a number of phases beyond 12. Accordingly, calculations of the relative current *TIF* and relative voltage *TIF* for different numbers of rectifier phases from 6 to 72 were made by the method previously described. These calculations assume

1. Both 60- and 25-cycle supply.
2. Load and circuit conditions, represented by I the direct current per secondary phase group, X the commutating reactance in secondary terms, and E_o the crest value of anode-to-neutral voltage of the transformer secondary winding, of such values as to give a ratio of IX/E_o equal to 0.081, corresponding to an angle of overlap of 25 degrees.

In addition to the principal curves, which are based on no voltage reduction by phase control, that is, a phase-control ratio of one ($PCR = 1.00$), some results of calculations are given for a phase-control ratio of 0.85 ($PCR = 0.85$). The results of these calculations are plotted in the curves of Figures 1 through 6. Figures 1 and 2 give the curves of relative current—*TIF* for 60-cycle supply based on the 1935 and 1941 frequency-weighting curves, respectively. Figures 3 and 4 give the corresponding curves of relative voltage—*TIF*, respectively. Figures 5 and 6 give the corresponding curves of relative current—*TIF* for 25-cycle supply. Examination of the curves of Figures 1 through 6 shows important reductions in both the curves of relative current—*TIF* and voltage—*TIF* as the number of rectifier phases is increased. However, because of the high peak in the 1935 frequency-weighting curve in the vicinity of 1,100 cycles, the 18-phase rectifier with a 60-cycle supply and a 36- or 42-phase rectifier on a 25-cycle supply are less favorable than the next smaller number of rectifier phases. Because of these circumstances, it has been considered generally undesirable to use 18-phase rectifiers on 60 cycles and 36 or 42 phases on 25-cycle systems. However, the 1941 frequency-weighting curve does not have this sharp resonant point and there is a gain in increasing the number of phases throughout the practical range of rectifier application.

Effects of Supply-System Harmonics and Rectifier Load Balance and Their Interrelation

Harmonics in the supply system may have important effects on rectifier operation, particularly in producing unbalance in current between the various phase groups. Unbalancing load between phase groups also has an important effect on supply-system harmonics. Because of this interrelation between harmonics in the supply and load balance, it is convenient to consider the subject in three parts:

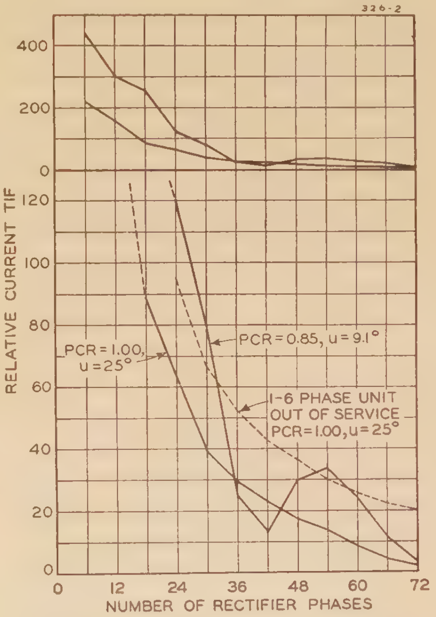

Figure 2. Effect of number of rectifier phases on relative-current *TIF*

Based on 60-cycle supply and 1941 frequency-weighting curve

Insert: Replot of principal curves to smaller scale

1. An infinite source with load unbalance.
2. An infinite source with harmonic voltages.

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* In addition, there is the 1919 frequency-weighting curve that remains in use for machine wave-shape guarantees. The 1919 curve is associated with the term "telephone interference factor;" the 1935 and the 1941 curves are associated with the term "telephone influence factor;" both terms have the same abbreviation of *TIF*.

3. A finite source with two or more rectifiers, such that the harmonics from one affect the voltage applied to another.

INFINITE SOURCE WITH LOAD UNBALANCE

If one six-phase unit of a multiphase rectifier* is removed from service, the harmonic currents in the supply are affected importantly, as pointed out previously. This unbalanced condition may be analyzed by the addition of

1. A fictitious six-phase load of such magnitude and phase relation when added to the actual loads as to provide a balanced multiphase rectifier.
2. A second fictitious load, the negative of the first.

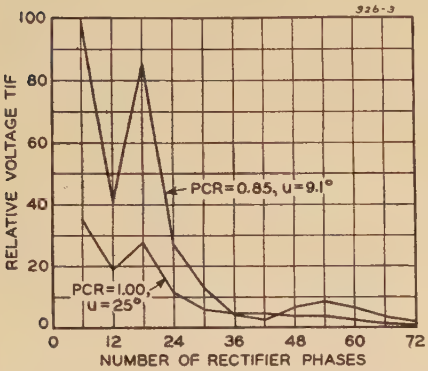

Figure 3. Effect of number of rectifier phases on relative voltage *TIF*

Based on 60-cycle supply and 1935 frequency-weighting curve *TIF*=(relative voltage *TIF*)*K''*

where

$$K''=\frac{I(\text{per cent } X)}{RI_{ac}}$$

I=D-c load current per secondary phase group

Per cent X=Transfer reactance in per cent, based on rated d-c kilowatts

R=Transformer ratio, primary to secondary line-to-neutral voltages

I_{ac}=Alternating current on primary side of transformer, rms value

Thus, this combination can be analyzed as

1. A multiphase rectifier of balanced positive load corresponding to the original number of phases.
2. A six-phase rectifier of negative load.

Both of the rectifier loads may be analyzed by the aid of the method previously described. The curves of Figures 1, 2, 5, and 6 also include some data giving the relative current telephone interference factor for a six-phase unit out of service in a multiphase rectifier. For convenience the curves have been plotted for a six-phase unit out of service with a current magnitude equal to that proportion of the total load. Actually, the harmonic currents should be increased because of the lower ratio of IX/E_0 that applies for such a condition.[3]

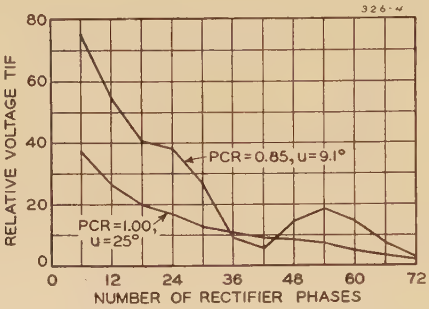

Figure 4. Effect of number of rectifier phases on relative voltage *TIF*

Based on 60-cycle supply and 1941 frequency-weighting curve

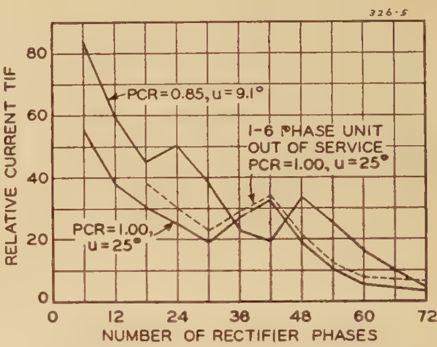

Figure 5. Effect of number of rectifier phases on relative current *TIF*

Based on 25-cycle supply and 1935 frequency-weighting curve

Actual current *TIF*=(relative current *TIF*)*K'* (see Figure 1 for *K'*)

INFINITE SOURCE WITH HARMONIC VOLTAGES

The effect of harmonic voltages in a source, particularly at the lower-harmonic frequencies, is to increase or decrease during the conducting period the average voltage applied to the various phase groups. The practical result of this action is to unbalance the currents between the various phase groups. The phase position of the harmonic with respect to the fundamental is different for different phase groups derived by transformers from the same supply voltage. In considering the relative phase position of the harmonic voltage of a phase group with respect to its fundamental-frequency voltage, it is necessary to consider not only the higher frequency of the harmonic but also the difference in symmetrical-component phase sequence. It has been shown,[6] for the condition in which the crest of the harmonic voltage bears the same relation to the crest of the fundamental in each phase, and the sequence of the fundamental is positive, that the harmonics of order $(3m+1)$ are of positive sequence and of order $(3m-1)$ are negative sequence, where m is any integer. Thus, the 5th, 11th, and so forth, harmonics are of negative sequence, and the 7th, 13th, and so forth, harmonics are of positive sequence. The effect of harmonics on the average d-c voltage of a phase group can be understood more readily by reference to the diagram of Figure 7. If harmonics in the supply are such as to produce fifth-harmonic voltage applied to one rectifier phase group, as shown by the solid line curve of Figure 7, the effect is to decrease the average value of voltage on that phase group during the conducting period. If a second phase group is displaced 30 electrical degrees at fundamental frequency from the first phase group, as in a 12-phase rectifier, the phase relation of the fifth harmonic to the fundamental in the second phase group is displaced 180 degrees from the phase position of the fifth harmonic with respect to the fundamental in the first phase group. This circumstance results from the fact that the phase relation of the fundamental is shifted 30 electrical degrees in one direction while the fifth harmonic, being of negative sequence, is shifted five times 30 electrical degrees in the opposite direction. Thus, if the fifth-harmonic voltage in the supply tends to decrease the average voltage of one phase group, it tends to increase the average voltage of the other phase group in the 12-phase rectifier.

If the harmonic voltage in the supply to a multiphase rectifier is shifted with respect to the fundamental, the harmonics in the secondary phase groups are shifted with respect to the fundamental, and the effects on load unbalance are altered. It is possible, therefore, that one transformer connection will be better from the standpoint of current balance than another, if harmonics of definite phase position with respect to the fundamental exist in the supply. In general, however, the phase position of the harmonic with respect to the fundamental will change at the rectifier location, and as a consequence there is little practical advantage in one transformer connection over another from the standpoint of current balance. The maximum effect of unbalancing between phase groups may be encountered in a 12-phase rectifier if fifth harmonics of appropriate phase relation exist in the power source. If rectifiers of more than 12 phases are supplied from a source with a fifth-harmonic voltage, the various rectifier units will carry loads between the two extremes for maximum load unbalance.

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* The term multiphase rectifier is used in this paper to mean rectifiers of more than six phases.

The maximum unbalance between displaced rectifier phase groups that can result from a fifth harmonic in the supply can be estimated from the equation:

$$\pm \text{ per cent } I_{un} = \frac{20 \text{ (per cent } E_5)}{\text{per cent regulation}} \qquad (1)$$

where

I_{un} = the unbalance current per rectifier in per cent of normal current per phase group

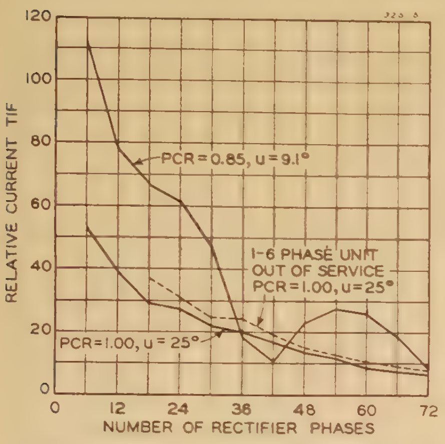

Figure 6. Effect of number of rectifier phases on relative current *TIF*

Based on 25-cycle supply and 1941 frequency-weighting curve

per cent E_5 = the fifth harmonic voltage in supply in per cent of fundamental

per cent regulation = the regulation of rectifier and transformer but not of common supply to the various phase groups, in per cent

Thus, the unbalanced current in per cent is two to five times the per cent fifth-harmonic voltage per cent in the source for the usual regulation conditions of from eight to four per cent.

The effect of a seventh harmonic in the supply is quite similar to that of a fifth harmonic. However, the seventh harmonic is usually of smaller magnitude and, therefore, has smaller effect on load balance. Furthermore, a greater magnitude of the seventh harmonic is required to produce the same amount of unbalance, because the uncompensated part of a shorter half-cycle of the harmonic must be averaged over the same conducting period.

Several years ago an analysis was made of a case where load unbalance was observed on a 12-phase, 3,000-kw rectifier. In this particular installation, four separate multianode tanks were supplied by a quadruple-zigzag 12-phase transformer connection. Harmonics in the supply voltage, both as to magnitude and phase position with respect to the fundamental, and the difference in regulation of the different phase groups were measured and the results compared with tests. The calculations based on the foregoing analysis indicated correctly which phase group should carry the heavier load. The magnitudes of the fifth-harmonic voltage and the unbalanced current are given in Table I.

The calculations were made from harmonic analyses of polar oscillograms of the supply-voltage wave, which gave both magnitude and phase position of the fifth harmonic with respect to the fundamental. For test 2, the phase position of the fifth-harmonic voltage was within four fundamental degrees of that position which theoretically should give the maximum unbalance for that magnitude of harmonic voltage.

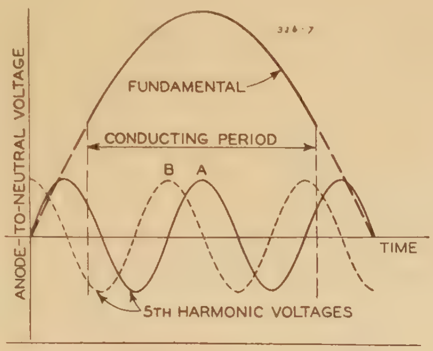

Figure 7. Diagram showing phase position of fifth harmonic to fundamental-frequency voltages for (*A*) maximum and (*B*) zero load unbalance of rectifier groups in 12-phase connection

Finite Source With Unbalanced Rectifier Loads and Harmonics

When multiphase rectifiers are operated from a supply system of negligible reactance, the unbalanced load resulting from the disconnection of one six-phase unit produces harmonic currents through the source corresponding to the fictitious negative six-phase load as previously pointed out. However, the outage of one six-phase unit does not affect the operation of the other six-phase units for the case of an infinite source. Consider now the case with a finite source. The effect of the harmonic current from the fictitious negative six-phase load corresponding to one unit out of service produces a harmonic voltage drop impressed on the other units which may be connected in parallel with the one that has been removed from service. Several effects may be noted. One effect of the harmonic impedance through the source is to divert part of the fictitious load current from the source to the other rectifier units. The effect may be viewed as being produced by the negative harmonic-voltage drops impressed on the remaining six-phase rectifier units as a consequence of the fictitious negative load. The result in any case is to reduce the harmonic currents flowing through the source to an amount that is less than would be expected from the greater equivalent reactance of the rectifier and supply system.[3]

Table I. Effects of Fifth-Harmonic Voltage on Rectifier Load Balance

Test	Fifth Harmonic Voltage	Unbalanced Current Per Phase Group—Per Cent: Calculated*	Unbalanced Current Per Phase Group—Per Cent: Measured
1	2.4	±6.5	±(6.5 to 7.6)
2	1.9	±5.6	±6.2

* From harmonic analysis of voltage wave.

From a practical standpoint the effect of these unbalances may be very important if they were allowed to exist. Thus, a 15 per cent fifth-harmonic voltage may produce sufficient unbalancing effect as to substantially remove the load from one six-phase unit and to nearly double the load on another. However, such conditions would normally not be allowed to remain because the voltage on the most heavily loaded six-phase unit would be reduced by phase control. This would have the effect of more nearly balancing the loads of the several six-phase units. The unbalancing effect, however, may be important if the momentary currents exceed breaker settings.

The effect of a unit out of service on a multiphase rectifier is, of course, to increase the wave-shape distortion of the supply and to increase the load unbalances. The results vary for the particular systems depending upon the number of units and reactance of the individual units and the reactance of the supply system at the harmonic frequency. The range of conditions is so great that it is scarcely practicable to generalize on the results that may be obtained. Reference, however, may be made to Marti and Taylor[6] and to the work of the Joint Subcommittee on Development and Research, Edison Electric Institute and Bell Telephone System, which has given considerable attention to this problem.[7–9]

They show that the increase in the $I \cdot T$ products for 30- and 60-phase rectifiers with a unit out of service varies from three to five times these factors for normal balanced conditions. Because of this, there is little advantage in using more than, say, 36 phases, unless the total rectifier load is large, or unless the possibility of resonance in the supply in the vicinity of 36th harmonic is anticipated.

Resonance in the Supply System

The harmonics produced by rectifier operation are of much greater importance if the supply system is resonant for one of the rectifier harmonics, the lower frequencies being more likely to be important from the standpoint of effect on apparatus and higher frequencies from the standpoint of inductive co-ordination.

Table II. Harmonics Present for Rectifiers of Various Phases

Harmonic	6-Phase	12-Phase	18-Phase	24-Phase
5	x			
7	x			
11	x	x		
13	x	x		
17	x		x	
19	x		x	
23	x	x		x
25	x	x		x
29	x			
31	x			
35	x	x	x	
37	x	x	x	
41	x			
43	x			
47	x	x		x
49	x	x		x

The harmonics that may be impressed upon the supply system for 6-, 12-, 18-, and 24-phase rectifiers under balanced conditions are shown in Table II for harmonics as far as the 49th. The number of frequencies of 49th harmonic and below that may be present for balanced operation are 16 for 6-phase, 8 for 12-phase, 4 for 18-phase, and 4 for 24-phase rectifiers. Thus, there is much less chance of resonance for rectifiers of the larger number of phases. However, if a six-phase unit is out of service, the harmonics that may be present are the same as for the six-phase rectifier, although the magnitudes are lower than for full six-phase operation. From the standpoint of chance for resonance when one unit is out of service, there is a difference between two 24-phase rectifiers if one is made up of four six-phase units or two 12-phase units.

Frequently capacitors are used in conjunction with large industrial rectifiers supplied over long transmission lines for the purpose of increasing voltage level, thus increasing the load power factor and the amount of power that can be handled. These capacitors may increase the opportunity for producing resonance, particularly at the time when a rectifier unit is out of service. Consequently, special attention should be paid to the harmonic problem for installations involving multiphase rectifiers and capacitors.

Capacitors

In themselves capacitors generate no harmonic voltages, but they affect harmonic voltages and currents from other sources only because of the relation of their harmonic reactance to that of the connected system. Large capacitors on the supply side of rectifier transformers tend to provide a short-circuit path for the higher harmonics incidental to rectifier operation. For the lower-frequency rectifier harmonics, large capacitors may form a resonant path with the source which may result in larger harmonic currents in the source and in the capacitor than in the rectifier transformers themselves. Increasing capacitor kilovolt-amperes beyond a certain value at a rectifier location will decrease voltage telephone interference factor. This is an effect with capacitors and harmonics from rectifiers that is analogous to that pointed out by Feaster and Harder[10] for the case of capacitors and harmonics from transformer magnetizing current.

Depending upon conditions, it may be theoretically desirable to modify the harmonic-impedance characteristic of a shunt capacitor so as to make it

1. Provide a short-circuit path for harmonics originating in the rectifier.

2. Provide an open-circuit path to a particular harmonic originating in the system.

The short-circuit path can be obtained by adding a reactance in series relation with the capacitor of such value as to tune in series resonance for the desired frequency. Thus a reactance of four per cent at fundamental frequency on the capacitor-bank rating is required for series resonance with the entire bank at fifth-harmonic frequency. Such a reactor will increase by four per cent the fundamental-frequency voltage impressed across the capacitor, a circumstance that should receive consideration in the design and application of the capacitor. To make the shunt capacitor operate as an open circuit to a particular harmonic frequency, the capacitor may be divided into two parts with a reactor of appropriate value placed in series with one of them. Thus for a parallel-resonant combination at the fifth-harmonic frequency, a reactor of 16 per cent at fundamental frequency on the bank rating would be required for connection in series with one half of the capacitor bank. This reactor would increase the fundamental-frequency voltage applied to that section of the capacitor by eight per cent.

Where large capacitors are connected in parallel with large multiphase rectifiers supplied by transmission systems, it is particularly desirable to give careful consideration to the harmonic problem. The effects for the lower frequencies should be studied analytically, and by field tests which should give, if possible, the magnitude of the fifth-harmonic voltage in the supply and the harmonic-frequency impedances. With this information available, the capacitor application should be studied to determine what characteristic at harmonic frequency should be employed. It appears possible but unlikely that in a few installations the subdividing of the capacitor into sections tuned for various frequencies will be found desirable.

If the capacitor is supplied through a transformer (or autotransformer) and connected to the bus which supplies rectifier transformers, the effect is equivalent to putting a reactor in series with the capacitor and thus to tune it for

Table III. Typical Preferred Minimum Kilowatt Rating for Multiphase 600-Volt Rectifiers*

Rectifier Units	Minimum Preferred Rating—(Kilowatts) Total Number of Phases 6	12	18	24	30	36	42
1,500 kw— 6 anodes	1,500	3,000	4,500	6,000	7,500	9,000	10,500
3,000 kw—12 anodes	3,000	3,000**	9,000	6,000**	15,000	9,000**	21,000

* Based on 6-phase transformers and phase shifters for the larger number of phases.

** Based on 12-phase transformers and phase shifters for the larger number of phases.

some (not necessarily integral) harmonic frequency of the supply system.

Available Number of Phases

Standard ignitron rectifiers in the 600–700 volt class are available in ratings up to 10,000 amperes. Typical minimum preferred kilowatt ratings for a given number of rectifier phases for large loads, as in electrochemical service, are listed in Table III, which is based on the use of 1,500-kw 6-anode and 3,000-kw 12-anode assemblies. The 3,000-kw unit is, of course, more economical than the smaller unit and should be used where total load, spare capacity, and similar considerations permit.

In industrial applications using smaller ratings, fewer anodes are required, and it becomes correspondingly more expensive to provide many phases. The possible combinations depend upon the load to be carried, the requirements for spare capacity, and the additional expense that may be justified for additional anodes. Table IV has been prepared to indicate the range and characteristics of combinations for various loads and number of phases. In some cases combinations with different number of transformers are listed for a particular rating. Since the cost of the installation will increase with the number of transformers, Table IV also gives an order of preference for the various combinations from the operating point of view aside from wave shape.

Table IV. Combinations for Various Loads and Number of Phases

Rectifier Rating (Kilowatts) 600–700 Volt Class	No. of Transformers 6-Phase	12-Phase	24-Phase
750	1		
1,000	1		
1,500	1		
2,000	1	1*	
3,000	1	1*	
5,000	1	1*-2	3**
6,000	1	1*–2	3**

* 12-phase operation by the use of one quadruple-zigzag transformer.

** 24-phase operation by the use of two quadruple-zigzag transformers and phase-shifting transformer.

Summary

The harmonic and load-balance characteristics of rectifiers and their complicated interrelations have been analyzed. The effect of source harmonics on rectifier load balance has been shown. Curves giving the effect of a wide range of rectifier phases on wave-shape distortion have been presented. The factors which should receive consideration in the selection of the number of rectifier phases have been outlined.

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A Vertical-Flow Compressed-Air Circuit Breaker and Its Application on a 132-Kv Power System

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Synopsis: The compressed-air circuit breaker for indoor power-house service is becoming very popular. However, its use for high-voltage outdoor service is still in the field-trial stage. Several high-voltage porcelain-clad circuit breakers of American design are now in service, and their performance is being watched carefully. A 138-kv 1,500,000-kva three-pole breaker has been installed at the Kittanning substation of the West Penn Power Company. Before installation its interrupting ability was checked to 5,900 amperes with full 132 kv on a single-pole unit and on a circuit with a transient recovery rate of 3,100 volts per microsecond. The mechanical operation was tested with the breaker in a heavily iced condition at −14 degrees Fahrenheit. The air-supply system was tested at −4 degrees Fahrenheit, and both were found to operate satisfactorily.

The breaker was installed on the most active line of the West Penn Power Company system. During the three months following its installation it had 41 operations, five of which were on faults. Its operation to date has been satisfactory in every detail, and it is hoped that this new device will meet operating requirements and reduce materially the time outage for servicing such apparatus.

I. Introduction

THE circuit-breaker art, almost from the time of its beginning, has depended upon the use of oil as an interrupting medium. Its use has permitted this country to develop a power system whose expanse and kilovolt-ampere concentrations are equaled no other place in the world. However, during the past four years, the interest in and the acceptance of compressed air as an interrupting medium has increased at an accelerating pace.

A new type of compressed-air circuit breaker for powerhouse indoor service, utilizing a transverse blast, has been developed to cover the range of kilovolt-amperes from 500,000 to 2,500,000.[1-3]

Possibilities of the high-voltage compressed-air circuit breaker for outdoor service in America have been carefully considered and found attractive, but the advantages and disadvantages could best be definitely established by outdoor field installation and trial. Therefore, a breaker was designed, built, and tested in the Westinghouse high-power laboratory[4] and installed in August at the Kittanning substation of the West Penn Power Company.

II. Application

West Penn Power Company's 1940 plans provided for a 132-kv three-bay extension to their major switching station segregated from the existing structure. The use of three oilless breakers to eliminate fire hazard and reduce the time element for servicing was the objective. Such breakers were not available to meet the construction schedule. However, it was decided that operating experience should be obtained with such breakers; this resulted in the placing of an order in January 1941 for one vertical-flow compressed-air three-pole fully automatic 1,500,000-kva 138,000-volt breaker which was to be installed on a circuit where the greatest amount of operating experience would be secured which might be representative throughout the system. The circuit selected was already controlled by an oil circuit breaker equipped with impedance networks, provided with carrier line-current relaying and included in differential bus protection. The breaker specifications, therefore, included capacitors to replace the impedance networks, arrangement for mounting carrier wave trap, and double-ratio double-winding double-core current transformers. The inherent design located the current transformers on the line side of the breaker which therefore left the major portion of this breaker connected to the bus and included in differential protection, which was considered satisfactory. The design is such as to permit future automatic reclosing and single-pole operation.

The circuit controlled extends over a 30-mile wood-pole line to a switching station where it interconnects with the Penn-

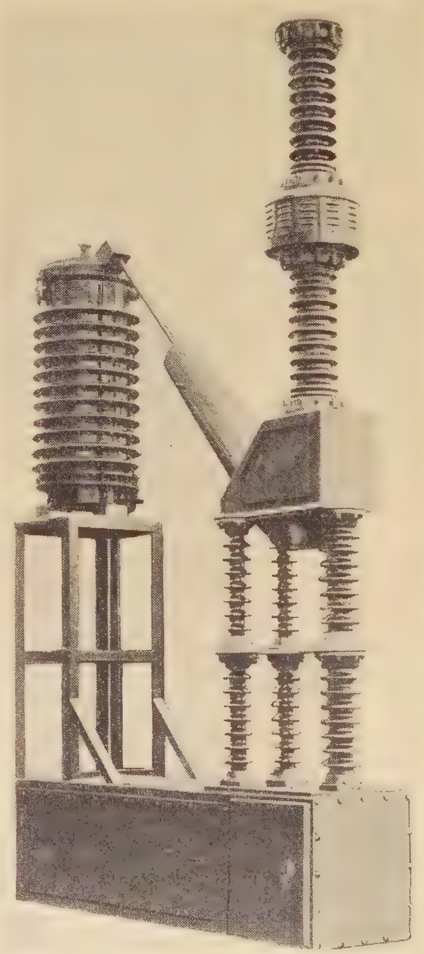

Figure 1A. Single-pole unit of a three-pole 138-kv 1,500,000-kva compressed-air circuit breaker

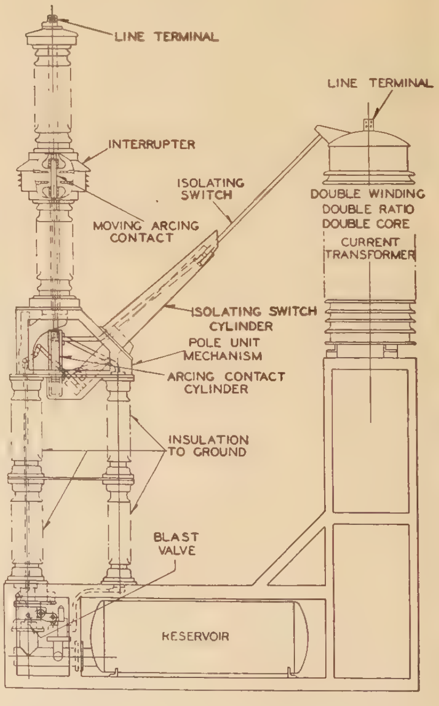

Figure 1B. Schematic drawing showing side elevation of the breaker in Figure 1A

Paper 43-51, recommended by the AIEE committee on protective devices for presentation at the AIEE national technical meeting, New York, N. Y., January 25–29, 1943. Manuscript submitted November 27, 1942; made available for printing December 21, 1942.

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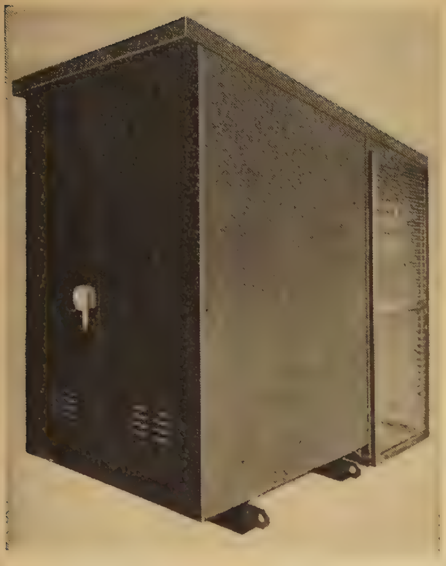

Figure 2A. Compressor house and 1,000 pounds per square inch storage system

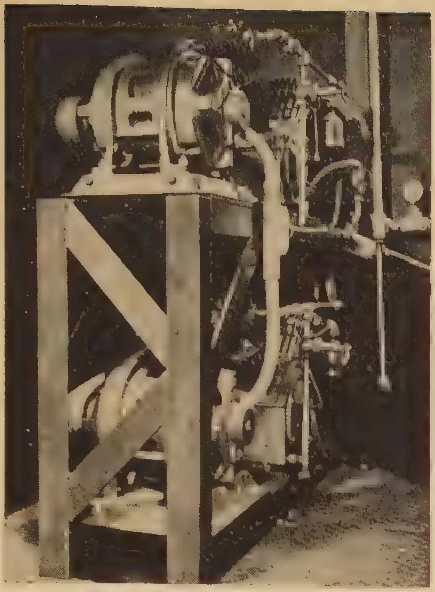

Figure 2B. Compressors and driving motors mounted on the left side of the center aisle of the compressor house

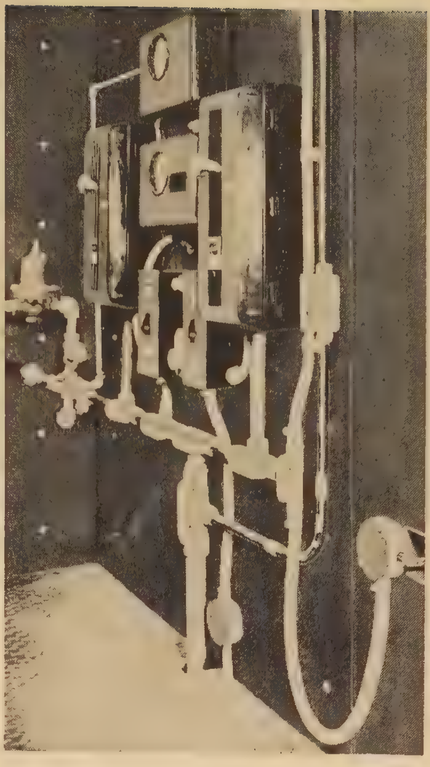

Figure 2C. Motor controls and pressure governors mounted on the right wall of the compressor house

sylvania Electric Company's system. From that point the circuit extends over 43 miles of wood-pole construction to the Keystone system of the West Penn Power Company at Ridgway, Pa.; thence again over 38 miles of transmission line, where it extends and interconnects at Warren, Pa., with the Pennsylvania Electric Company's system.

The application of this breaker is generally in line with recommendations of the action of January 27, 1941 of the Joint Committee of the Edison Electric Institute, Associated Edison Illuminating Companies, and the National Electrical Manufacturers Association, which was later followed and agreed upon in their October 24, 1941 decision, recommending field service installations on a selected set of ratings. This development holds promise of effecting savings in critical materials.

III. Description

Figure 1 shows both a picture and a schematic diagram of a single-pole unit of the 138-kv breaker. The air-storage reservoir is mounted in a simple frame which also houses the blast valve and the valves which control air for opening and closing the breaker.

At one end of the frame is mounted a porcelain clad Inerteen-filled wound-primary current transformer. The double secondary, double winding, and double core provide for bus differential protection and standard line protection, using only one transformer unit per phase. The transformer forms one line terminal of the breaker and supports the stationary contact of the isolating switch. At the other end of the frame are mounted three tubular porcelains which provide insulation to ground and form a tripod support for the pole-unit mechanism. One of these porcelains carries the air blast; the other two carry air for opening and closing the contacts respectively.

The pole-unit mechanism consists essentially of two simple cylinders and pistons. The one mounted vertically operates the arcing contact within the interrupting device, and the other mounted at an angle operates the isolating switch. Both are very fast and pneumatically interlocked, so that the circuit is always opened on the arcing contact and closed on the isolating switch.

The double-orifice vertical-flow interrupting element described fully in another paper[5] is supported by the pole-unit-mechanism housing between two identical porcelain columns.

With the breaker in the closed position, the circuit is complete from the top of the upper interrupter porcelain through the interrupter, the pole-unit mechanism, the isolating switch, and current transformer.

Bleeder valves are provided to allow a continuous trickle of dry air through all three porcelain columns, thereby preventing breathing and keeping their inner surfaces in better operating condition.

Table I. Interrupting Tests on a Single-Pole Unit of a Three-Pole 138-Kv 1,500,000-Kva Compressed-Air Circuit Breaker

Tests June 5, 1942, at the East Pittsburgh High-Power Laboratory. Witnessed by Engineers of the West Penn Power Company

Test	Applied Voltage (Kilovolts)	Recovery Voltage (Kilovolts)	Amperes Interrupted (RMS)	Amperes Closed (Crest)	Reservoir Pressure (Pounds Per Square Inch)	Arcing Time (Cycles)	Circuit-Transient Recovery Rate (Volts Per Microsecond)	Frequency Cycles Per Second	Type of Operation
*1	120	114	1,410		350	0.8	1,100	60	O
2	120	116	1,150	1,560	350	0.7	1,100	60	CO
3	120	112	2,540		350	1.2	1,550	60	O
4	120	108	2,030	4,330	350	1.5	1,550	60	CO
5	120	110	3,160		350	1.0	2,000	60	O
6	120	107	2,820	7,150	350	0.8	2,000	60	CO
*7	120	109	4,830		350	1.7	2,600	60	O
†8	120	108	4,400	10,600	350	1.3	2,600	60	CO
9	44	41.5	2,400		350	1.4		60	O
10	88	81.0	5,650		350	1.6	2,300	60	O
11	88	80.0	4,800	11,800	350	1.0	2,300	60	CO
*12	88	82.0	6,750		350	1.6	2,500	60	O
13	88	80.5	5,840	8,050	350	1.7	2,500	60	CO
14	88	83.0	7,000		350	1.0	2,500	60	O
15	33	30.0	5,410		350	1.0		60	O

* Oscillograms shown in Figure 4. † Oscillogram shown in Figure 5.

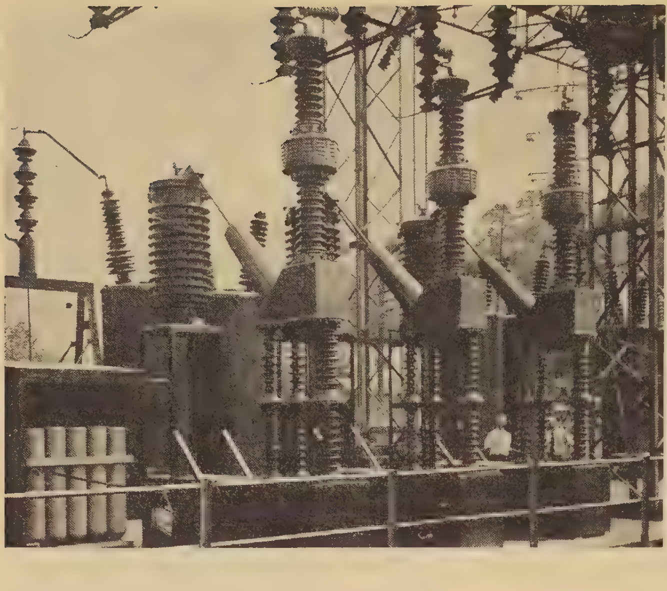

Figure 3. A three-pole 138-kv 1,500,000-kva compressed-air circuit-breaker installation at the Kittanning substation of the West Penn Power Company

The three poles are mounted separately and are mechanically independent but are simultaneously electrically controlled from a single housing which contains all relays, gauges, and so forth. Their operation is synchronized to within a cycle, and they may be converted easily to provide high-speed reclosing for either segregated or three-pole service. All control wiring is enclosed within the tank enclosure and supporting structure.

Figure 2 shows the air-supply system which consists of an insulated heated house containing two 1,000-pounds-per-square-inch compressors separately driven and operated independently or in parallel, each feeding through separate coolers into a battery of five 1.5-cubic-foot storage bottles. These bottles are connected through another cooler to a second battery of five bottles. All are used as storage capacity at 1,000 pounds per square inch from which air is supplied through a pressure-reducing valve to the breaker reservoirs where it is stored at 350 pounds per square inch. By compressing air to 1,000 pounds per square inch, cooling, and then expanding to 350 pounds per square inch, air sufficiently dry for satisfactory operation of the breaker is secured. Preparations are being made to measure periodically the moisture content of the air under varying atmospheric conditions.

The air-supply unit is mounted adjacent to the breaker, as shown in Figure 3, and connected by means of a one-inch copper air line to the middle pole unit from where it distributes through the respective control valves to each of the two outer pole units. From the central-pole control cabinet, air passes through a protective valve which is arranged to allow air to flow forward freely after the pressure has reached a predetermined value, but to limit the flow in the forward direction in case it exceeds a predetermined amount, which would be the case with a fouled valve or burst connection. A check valve is also incorporated in this protective scheme to guard against a reverse flow of air.

Three pressure-responsive switches are connected to the common air line to provide signals guarding against the functioning of the breaker with insufficient air pressure. One switch provides alarm when pressure drops below 250 pounds per square inch, another opens the closing circuit when pressure drops below 240 pounds per square inch, and the third opens the tripping circuit when the pressure drops below 225 pounds per square inch. These switches are so connected in the control circuit that, once an operation is initiated, it will be completed without interference from the pressure switches. Settings are such that, if there is sufficient pressure to allow closing, there will be also sufficient pressure to enable the breaker to open immediately in case of a fault.

IV. Factory Tests—Interrupting Tests

One pole of this breaker was tested in the Westinghouse high power laboratory[4] and witnessed by representatives of the West Penn Power Company. Table I shows this series of 15 short-circuit tests, including both opening and closing–opening operations with 120 and 88 kv respectively across the single-pole unit. The maximum current interrupted at 120 kv was 4,830 amperes, and 7,000 amperes at 88 kv. There was practically no visible disturbance during the opening operation of these tests, but a limited amount of arcing was noticeable at the final closing position of the isolating switch. The air discharge on opening operations was quite noticeable but not at all serious and did not increase with the increase of kilovolt-amperes interrupted. After the tests, the arcing and isolating contacts were all inspected and found to be in excellent condition.

Figure 4 shows three typical oscillograms of the opening operations.

Figure 5 shows a typical oscillogram of a closing–opening operation.

Table II shows a series of six opening operations with 132 kv across a single pole of the 138-kv breaker. The reactance of the circuit was adjusted to give 4,800 amperes with a circuit transient recovery rate of 2,800 volts per microsecond. The pressure in the reservoir was decreased after the first two tests, so that on the last test 4,400 amperes were interrupted with 225 pounds per square inch.

Figure 6 shows the magnetic and cathode-ray oscillograms of a single-pole unit interrupting 5,900 amperes with full 132 kv applied to its terminals. The circuit transient recovery rate was 3,100 volts per microsecond. The arcing time was 1.2 cycles.

These tests have not extended the limits of kilovolt-amperes interrupted on this type of interrupter.[5] However, they were made to demonstrate the adequacy of this breaker for 138-kv 1,500,000-kva service.

V. Mechanical Tests

The high-pressure air system was tested at 2,000 pounds per square inch, and the low pressure reservoirs at 800 pounds per square inch. The storage tanks met the state safety code and inspection requirements and carried their approval before assembly in the breaker.

At 350 pounds per square inch, the closing time for the isolating switch is approximately one-fourth second. The

Table II. Interrupting Tests With 132-Kv Initial Voltage, 60 Cycles, Applied to a Single-Pole Unit of a Three-Pole 138-Kv 1,500,000-Kva Compressed-Air Circuit Breaker

Test	Recovery Voltage (Kilovolts)	Amperes Interrupted (RMS)	Reservoir Pressure (Pounds Per Square Inch)	Arcing Time (Cycles)	Circuit Transient Recovery Rate (Volts Per Microsecond)
1	121	4,880	350	0.8	2,800
2	120	5,100	350	0.8	2,800
3	120	4,600	300	1.0	2,800
4	120	5,000	275	1.1	2,800
5	120	4,700	250	0.8	2,800
6	118	4,400	225	1.6	2,800

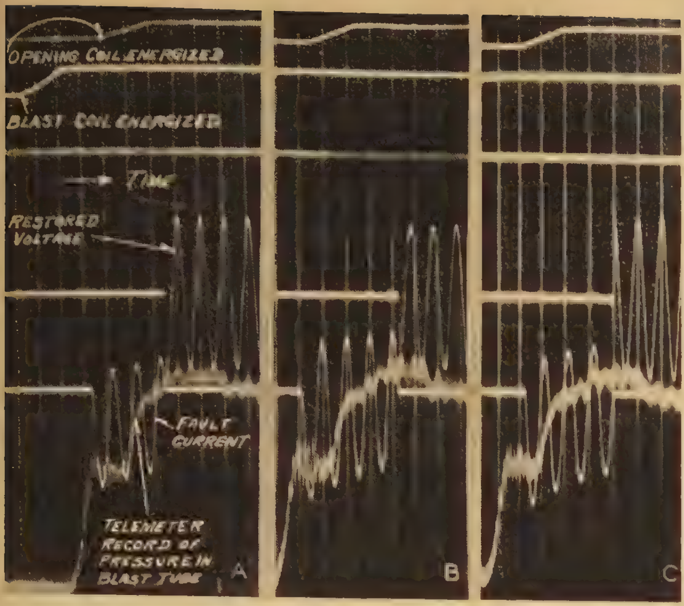

Figure 4. Magnetic oscillograms from Table I

Currents and voltages—rms (AIEE Standards)

A. Fault current 1,410 amperes; recovery voltage 114 kv

B. Fault current 4,830 amperes; recovery voltage 109 kv

C. Fault current 6,750 amperes; recovery voltage 82 kv

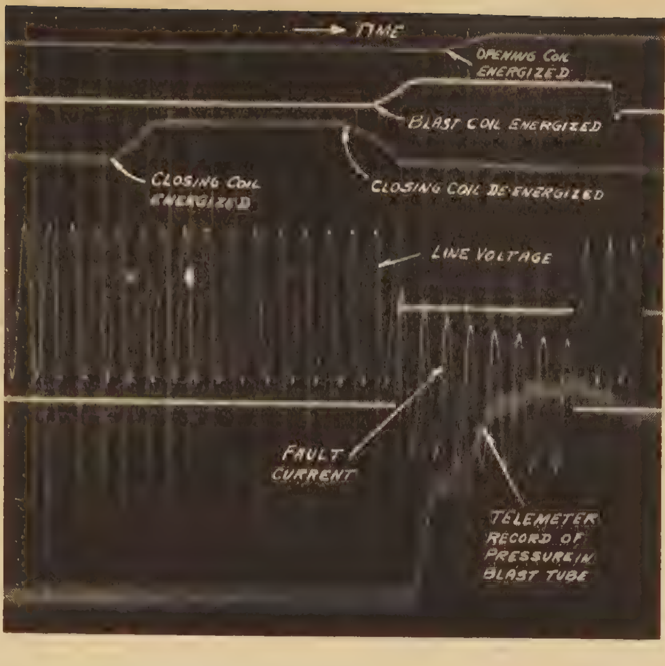

Figure 5. Magnetic oscillogram from Table I

three isolating switches reach the closed position within one cycle of each other. The time from rise of blast-coil current to parting of the arcing contacts is approximately six cycles. The three arcing contacts part within 0.6 cycle of each other. The spread is increased 0.1 cycle as the operating pressure is decreased to 270 pounds per square inch. The opening of a closing–opening operation increases the spread between the arcing contacts by another one- or two-tenths cycle.

VI. Low-Temperature Tests

The operation of the air-supply system at low temperatures was checked by placing it in a cold room with subzero temperature and operating it periodically for several days. Air was supplied to the compressor from outside the cold room, so that moist summer air at 85 degrees Fahrenheit was being compressed. The connection necessitated leaving ajar the door of the compressor cubicle; as a result, the temperature in the compressor cubicle reached 16 degrees Fahrenheit when the cold room temperature was −4 degrees Fahrenheit. At the end of the test, the air in the storage system was discharged through the pressure-reducing valve, with no tendency to foul its operation.

Everything functioned normally except a check valve in a cooler, which failed several times to prevent pressure from getting back to the compressor. This produced no evidence of trouble and was detected only by opening the drain valve on the compressor.

Figure 7 shows a single-pole unit of this breaker iced in a cold room at −14 degrees Fahrenheit. The tests at low temperatures extended over five days. However, in order to check its operation under the most severe conditions, the compressor was placed outside the cold room and moist warm summer air was pumped directly into the breaker, without going through the usual stages of compressing and then re-expanding in order to eliminate the moisture. Warm water

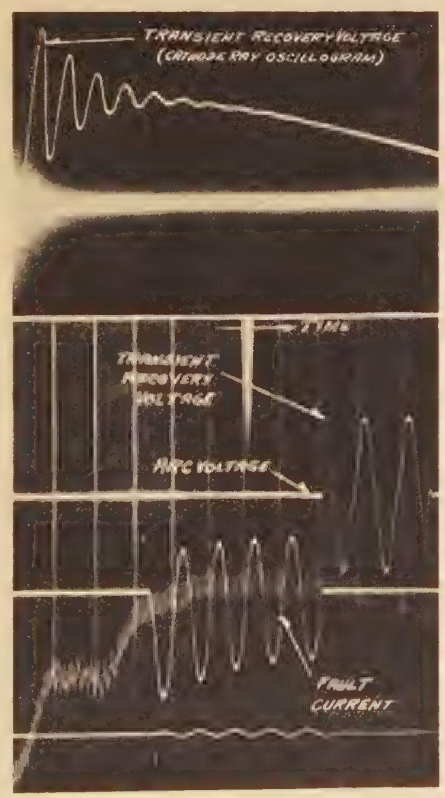

Figure 6. Cathode-ray and magnetic oscillogram of a single-pole 138-kv breaker operating with 132 kv across its terminals

Fault current 5,900 amperes; applied voltage 132 kv; circuit transient recovery rate 3,100 volts per microsecond

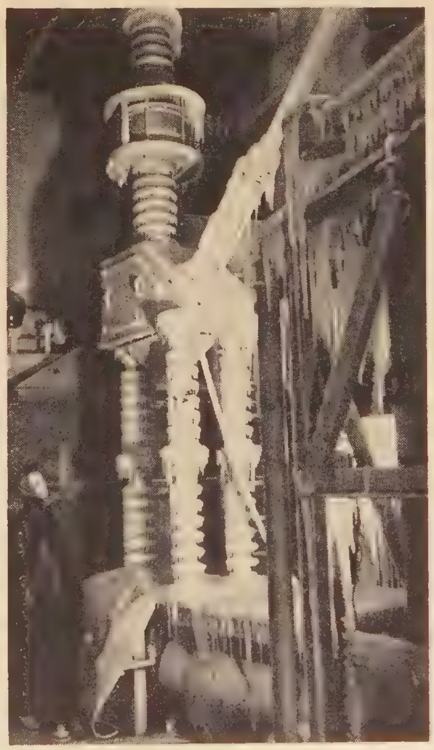

Figure 7. A single-pole 138-kv compressed-air circuit breaker iced for mechanical and electrical tests at −14 degrees Fahrenheit

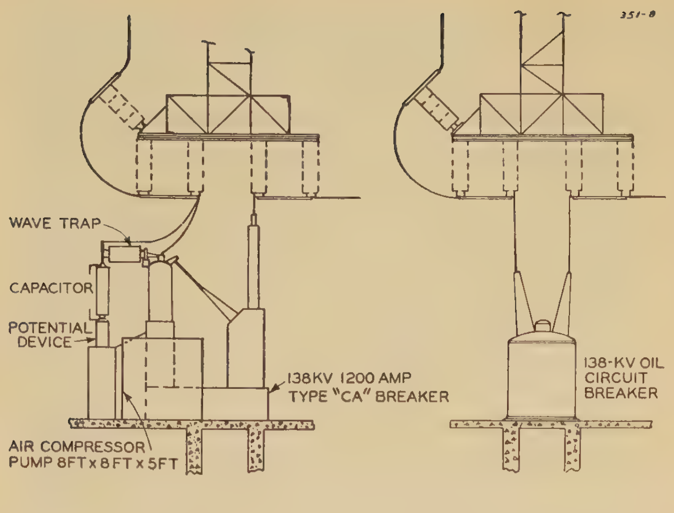
351-8
WAVE TRAP
CAPACITOR
POTENTIAL DEVICE
138KV 1200 AMP TYPE "CA" BREAKER
AIR COMPRESSOR PUMP 8FT x 8FT x 5FT
138-KV OIL CIRCUIT BREAKER

Figure 8. On the right is shown an oil circuit breaker located in the switchyard, and on the left is shown the way in which the compressed-air breaker and its accessories were arranged after the oil breaker was removed

was directed on the guide and packing gland through which the disconnecting blade slides, while the breaker was opened and closed. Thus the valves and all operating parts were thoroughly wetted. The temperature was then dropped to −14 degrees Fahrenheit and, by means of an intermittent spray, the breaker was iced as shown in Figure 7.

Upon energizing the opening valve, the operation of the contacts appeared normal, and the isolating blade apparently functioned as though no ice at all were present. Its normal average speed of 24 feet per second was recorded.

While the breaker was still iced, 132 kv was applied across the insulation ground for two minutes. Some corona was observed on the ends of icicles, but no flashover resulted.

VII. Installation

The complete breaker was trucked about 45 miles to the Kittanning substation on July 16, and, after assembly of the pole units in the vicinity of its permanent location, the oil breaker was removed, and within seven days the air-blast breaker was installed, miscellaneous changes were made, and it was tested and placed in service. Approximately 1,400 man-hours were spent on the entire job. The miscellaneous changes consisted of removal of the capacitors from the breaker assembly, rearrangement of controls, fittings, and so forth. The manufacturer's design met all general specifications, but the three capacitors were relocated and connected to the station bus to provide bus potential for all 132-kv relaying, synchronizing, and so forth. The original capacitor and carrier wave-trap installation was retained. Figure 8 shows an installation comparison of the oil breaker with that of the air-blast unit. The latter shows the capacitor with one wave trap in place as originally planned. All three poles of the breaker and the air-compressor unit have been installed within the oil-circuit-breaker bay, all of which are located on an elevated concrete platform.

The complete functioning of the entire unit was checked with the following results. With the air compressors out of service and the air-storage system normal, six opening operations and five closing operations were obtained in less than six minutes. It is recognized that this sequence should be used only in an emergency, since the charging of the breaker reservoirs from low bottle pressure decreases the effectiveness of the drying cycle. However, after operating the breaker with improperly dried air, its Megger readings had not decreased, and the breaker was placed on the 132-kv line without evidence of trouble. With the high-pressure system disconnected, the breaker completed an opening operation, followed immediately by a closing–opening operation, using air only from the pole-unit reservoirs.

It has not been found convenient to make short-circuit tests on the system because of the importance of the interconnection and because of the duty to which the breaker can be subjected being limited to about 1,000,000 kva. During the three months following its installation there was a total of 41 breaker operations, five of these being automatic as a result of line faults. All of these have been faults to ground with current values limited to not more than 3,000 amperes. All operations have been quite satisfactory with no noticeable system disturbance and without evidence of any breaker distress.

Included in the preceding operations there have been a number of cases where the charging current of the unloaded open end line has been interrupted with no noticeable disturbance.

The trickle of air through porcelain columns requires about one hour's operation per day for one compressor. A rather complete record is being kept of all work pertaining to the breaker, and no definite maintenance schedule has been formulated as yet. Air-compressor inspection will be governed by actual operating hours.

VIII. Conclusion

We are quite hopeful that this new device will meet operating requirements and reduce materially the time outage for servicing such apparatus. The West Penn Power Company's future breaker purchases will be governed to a large extent by the performance of this unit, and it is hoped that other utilities may benefit and take advantage of this experimental installation.

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Rural Electrification Engineering and Electroagricultural Engineering

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THE great interest shown recently by electrical engineers in rural electrification engineering and electroagricultural engineering is demonstrated by the fact that in the October 1942 issue of ELECTRICAL ENGINEERING there appeared a paper which was entitled "New Engineering and a New Industry," presented in December 1941 before the American Society of Agricultural Engineers. The statement made therein that "before 1935 there was no rural electrification engineering," which was misinterpreted by some readers, was not made to discredit the work of anyone in electrifying farms, but referred to the fact that there was no specific branch of engineering that could be called "rural electrification engineering." In fact, we all consider rural electrification engineering as a part of distribution engineering in general, and the latter is likewise a very young branch of engineering. As a matter of fact, very few spoke of "rural electrification." In most of the literature prior to 1935 you will find only the term, "rural line extension." The following statement is from the National Resources Committee report in "Technological Trends and National Policy," 1937, page 284:

> "Electric distribution grew as America grew. No one could have possibly predicted the rapid growth of American cities or American industries. During that rapid period of growth, any systematic planning of distribution for several years ahead became out of date on short order. Furthermore, during the rapid growth of the industry, when millions upon millions were spent on generation and transmission, practically all the attention of engineers and manufacturers was concentrated on these two fields. The engineers were so wrapped up in generation and transmission that many of them considered distribution as too lowbrow to merit their attention. Distribution was frequently left to the lineman, purchasing agent, and storekeeper, and many distribution systems grew up to give the impression of crazy quilts, without any apparent logic either in circuit sizes, voltages, or locations, and sizes of transformers."

Distribution engineering really started with the introduction of the a-c network. Rural electrification engineering is a branch of distribution engineering, but the very fine engineering developed for networks, could not be applied to it. Those who had to compute rural circuits had to, and, to a great extent, still have to, use the methods developed by that branch of engineering to which most of the engineering brains of the last 30 years or so has been devoted—transmission engineering. Now, when on a transmission system the short-circuit current, the voltage regulation, and the stability are computed, the results indicate where breakers are needed and what kind, where relays are needed and what kind, where voltage regulators are needed and what kind, where reactors or capacitors are needed and what kind. The equipment can generally be found in a catalogue, and, if you have the money to buy it, even if each breaker costs $130,000, it can be ordered. You can also assume that there will be operators and operating engineers, even specialized engineers who are qualified to operate and maintain the large breakers, the fine relays, the elaborate recording and metering devices, and so forth. When the same method of computation is applied to rural circuits, and when the results are obtained, what do you have? Probably just a fuse, and you hardly have enough money to put in a fuse every place you think there should be one. And in many cases you may not put in a fuse—you will take a chance on getting along without it, because it is practically impossible to co-ordinate fuses as accurately as to co-ordinate relays. And you know that there will be no one but a lineman to operate and maintain. The importance of continuity of service for farming may be far more pronounced than for domestic use. Previously it took two to three weeks to compute the short-circuit current of a rural system, and, after the computation was all done, most of the results could not be used advantageously, since you had to do the best you could with existing equipment. So we first proceeded to reduce the mathematical analysis to existing conditions, with the result that an engineer can now do the job in one day. Second, we were able to show the manufacturer that a good market exists for a good circuit breaker for use on rural distribution circuits. There was only one on the market, but the manufacturer of this breaker had vision and kept improving it to meet the specific requirements of the primary rural circuit, with the result that Rural Electrification Administration systems alone bought over 5,000 such breakers in one year, and practically all systems that have them report that they are satisfied with their performance, because the breakers have saved them many extended interruptions. Other manufacturers have developed small breakers, a few of which are already in operation, but the war stopped further activity. We will need better and cheaper primary circuit breakers, both single-phase and three-phase, with a greater degree of co-ordinating flexibility. Many additional improvements are still needed to give more perfect co-ordination. This is what we call the first chapter of rural electrification engineering. It is by no means complete. (In electrical engineering we are accustomed to being always a few years ahead of the textbook.)

We consider the work done on telephone co-ordination as the second chapter of this specific branch of electrical engineering.

The voltage problem which is now in the process of study, will probably form the third chapter. We have already learned a lot that we did not know before, and some of it, we hope, will soon be ready for presentation to the profession, but it is a long and difficult job, and the analysis no doubt will bring out the need for new equipment. In this whole field of engineering there are not enough operating data for establishing the foundation for all future designs based on Kelvin's idea of balancing fixed charges against variable charges. The systems are too young. But enough data are already available to indicate the need for changes in equipment design. For instance, we already know that rural distribution transformers should have lower core losses. There is hardly any secondary distribution in rural areas; the consumers are so far apart that practically every one must have an individual transformer. Most of the rural systems use purchased energy, and in most cases the cost of energy represents a very prominent item in the total annual charges.

Paper 43-61, recommended by the AIEE committee on domestic and commercial applications for presentation at the AIEE national technical meeting, New York, N. Y., January 25–29, 1943. Manuscript submitted November 16, 1942; made available for printing December 21, 1942.

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This is in general an entirely different picture as compared to an urban distribution system.

There are innumerable similar items of difference. But what has been said here will be sufficient to stimulate the electrical engineer to devote some time to this new specific branch of electrical engineering and convince the electric-equipment designer that different and new equipment is needed for this field. The market is now large enough to justify these specific studies. For example the following are a few things that manufacturers could devote time to developing: smaller transformer with very low core loss; cheaper, but reliable lightning arresters, based on the fact that the short-circuit current on rural systems is practically never over 1,000 amperes; cheaper meters, more rugged, even perhaps at the sacrifice of a bit of accuracy; simple, cheap, portable instruments to measure ground resistance, without bothering too much about extreme accuracies; insulators that will stay put on the pin and not give radio interference; portable testing instruments that are safe and simple to operate; a simple portable oil tester and filter; new voltage-regulating equipment will no doubt be needed, but the available data are as yet inadequate for definite functional specifications.

There are hundreds of thousands of miles of rural circuits, single-phase, Vee and three-phase, 7,200 volts, all with grounded neutral. Single-phase circuits without any neutral at all are in existence here and there, the earth serving as the return path for the circuit. Each transformer is connected from the phase wire to ground. If there should be a break in the grounding wire or wires any place between the transformer and the ground, there would be practically full voltage across the break. This is indeed dangerous. But we have not heard of any accident resulting from this type of failure. A neutral conductor, which is assumed to be at ground potential, may be more dangerous than a 220-kv line, since adequate precautions always are taken when handling 220 kv. But we must know more about it, and the only way to find out is first to exhaust analytical methods, based on reasonable assumptions that are again based on experience, and then to do a lot of testing. There may be a voltage gradient on the surface of the soil from the pole out, and it will be necessary to establish what that voltage gradient is, because 100 volts may kill a horse. I am not expressing any opinion for or against the use of such circuits; I merely want to emphasize the importance of ground testing on a large scale. Where there is a neutral conductor, it may carry only 30 or 40 per cent of the amperes, the earth carrying the rest. And where there are amperes, there are volts, and here too the voltage gradient should be tested. I know of course that such tests need a voltmeter and not a ground meter, but, if everyone had enough ground meters to establish the ground resistance in advance, the voltage gradients could almost be predetermined by computation. Besides, what are a few ohms of resistance in the life of a young lightning stroke that can jump across half a mile of space with the greatest of ease? Some tests will have to be carried on to determine ground inductance and capacitance. So you see how much engineering there is still to be done on the item of grounding alone before we are ready to say from an engineering viewpoint that we know the characteristics of a rural distribution circuit. This ought to be enough to satisfy those who think that "rural electrification engineering" is as old as Benjamin Franklin.

For supplying electric energy to farms in the less sparsely populated territory, beyond the economy of line extensions, there will be needed small self-contained simple rugged generating units, each to supply one or several farmers. It may be possible that plowed-in underground cable can be used, or that some other new method can be developed for serving a group of farms located in an isolated area.

Constant current primary with constant potential supply and constant potential load, as proposed by Percy H. Thomas and others, deserves serious consideration. Further study may prove its applicability to many conditions in rural areas, and, if it does, new equipment will have to be developed for the purpose. Conditions in the future will no doubt call for underground primary systems, and for these conditions a transformer will be needed which can be put right in the ground without any manholes or boxes. Such a transformer already has been tried out in Canada, but further development will be needed.

During the two years from the middle of 1940 to the corresponding period of 1942, joint development work has continued between the Rural Electrification Administration and the Bell Telephone System to determine the practicability of providing telephone service to rural communities by carrier channels superimposed on power distribution lines. Substantial progress has been made in solving a number of the technical problems encountered. Laboratory models have been made of apparatus which would be required at subscribers' locations and telephone central offices to render this service. Preliminary models also have been made of the apparatus which must be added to the power line, not only at the points where the connections are made to the subscribers' stations and the telephone central offices, but also at other points, in order to provide a satisfactory communication channel.

Considerable work remains to develop the apparatus into good commercial form and to determine from a field trial the answers to the many operating problems which would be encountered in rendering a service of this type. The status of the work recently has been reviewed jointly by the Bell Telephone System and the Rural Electrification Administration, and a decision reached that the work should be suspended for the duration of the war. This helps to meet the present need for concentrating on the war all possible resources, including technical men, critical materials, and manufacturing effort.

Problems of radio interference also occur in rural areas. A reasonably good locator has been developed by Rural Electrification Administration engineers in co-operation with a manufacturer. Several hundred such instruments are now being used with reasonably good results, but improvements are needed. As "telephone interference" was changed to "telephone co-ordination" by co-operative efforts of utility engineers and telephone engineers, so should "radio interference" be changed into "radio co-ordination" by co-operative efforts of the engineers of the electric utilities, the broadcasting companies, and the radio manufacturers.

So you see: The rural circuit is not just a pole, a guy, a little insulator, a little transformer, and a little fuse. It calls for the most painstaking engineering analysis. And when I speak of engineering, I will repeat that anything not based on a solid foundation of physics and mathematics is not engineering.

We now come to electroagricultural engineering. When the statement was made that there was no electroagriculture, it was not meant that no one ever used electricity on the farm. And when it was said that there was no electroagricultural engineering, it did not mean that no engineer ever tried to apply engineering to the use of electricity for farming. Again, it was meant that as yet there is no specific branch of engineering that could be called electroagricultural engineering, and there was no specific industry that could be called electroagricultural industry. As a matter of fact, these things are so new that it is difficult to say now what the subject of the first chapter of "Electroagricultural Engineering" should be. Even this will have to be developed as we go along. No more will therefore be said here about the engineering possibilities, and the discussion will limit itself to the industrial possibilities. Here we know of some definite needs, and this is addressed primarily to progressive American manufacturers who look ahead, find out what will be required, and proceed to build it and put it on the market.

Electroagricultural engineering is closely associated with rural electrification engineering, because, for example, the grounding problem is probably more important in connection with the fire hazard on the farm than in the city. A faulty ground may cause a fire, and, when a fire starts on the farm, the results are frequently disastrous. A simple cheap ground meter, suitable for the convenient use of every electrician, should be developed. Incidentally, the study of lightning protection of farm buildings and perhaps the development of more competent equipment for this purpose likewise invite some activity on the part of the electrical manufacturers.

While we are on the subject of hazards, we may as well consider the electric fence which is important in wartime, because less critical material may be used. The diversity of the types of control on the market is so great as to be bewildering, and many of the types are definitely hazardous. A committee of the National Electrical Safety Code has been engaged in the study of this subject, but, before quantity production is possible, some work by an AIEE committee and a National Electrical Manufacturers Association committee will no doubt be in order.

When large-scale rural electrification started, all efforts were concentrated on bringing into the farm the minimum amount of civilized comfort such as electric lights, radio, a flat iron, a water pump, a washing machine, perhaps a hot water heater, and here and there an electric range. This was as it should have been. The activity should continue, and improvements should be introduced. Illuminating engineers should study the problem of lighting for the farm, the farm home, the farmyard, the barn, and soforth, and the specific requirements of the circuit. This should possibly be made a project by the Illuminating Engineering Society. Lighting problems on the farm are different and call for different treatment.

An important item of comfort to which manufacturers should devote their attention is the development of a small, cheap, but very reliable water pump, in order to make it possible to bring running water into the poorest farm home. I consider this to be as important as the electric light. Public health authorities are unanimous in their opinion that running water in every home would reduce sickness in the countryside.

Recently there has come about a realization that every effort should be made to develop equipment to make it possible for the farmer who has electricity to use that electricity for real farming, to get more revenue out of his farm, to produce more or better food, to preserve food, and to make it unnecessary to let good food go to waste. This is real electroagricultural engineering. As was brought out in the paper previously referred to, some wonderful work has been done along this line by men of science in the colleges and in other laboratories, but it has not been possible as yet to make the result of their labor available to all the farmers in the country, because there is as yet no specific industry which can manufacture the items as quantity production. There is already some evidence of the beginning of such an industry. What already has been done only helps to demonstrate what we may expect in the future when a real electroagricultural industry is established, using quantity production methods for making low-cost but reliable equipment that the small farmer can use to get a little more revenue out of the electricity that he already has or will have. Most of the items already on the market are too expensive for the small farmer. They are intended for the more prosperous farmer, and our main interest should be to make the small farmer more prosperous.

At various times individuals have endeavored to list the electric equipment and appliances suitable for both the farm and the farm home. Some of these lists exceed the 350 mark. State Agricultural Colleges, the United States Department of Agriculture, and others have been responsible in large measure and have worked exceedingly hard for many years on the research and development of electric applications. It must be said that very few of these have found their way to the farm in large numbers. It is most difficult to get one's teeth into the real meat of these applications, for, although much work has been done, yet little specific information, either on statistics or accomplishments, has been assembled. At first thought this seems strange, because so many of these can be catalogued as income-producing equipment which has been proved profitable to the owner.

The year 1941 was an exceptional year in manufacturing and in the sale of all farm equipment, electric and nonelectric. It exceeded in volume any previous year, and the sales value of it was 36 per cent greater than 1940, and 34 per cent above the high mark set in 1937. Electric farm equipment sold for both the years 1941 and 1940, and the increase in favor in 1940 are shown in Table I.

It will be noted that the electric equipment manufactured and sold in quantity is confined to a few items. Those items assembled and released by the Bureau of the Census include only milking machines, cream separators, milk coolers, domestic pumps, water systems, and brooders. It should be remembered that the figures shown are for years when the sales of these units were exceptionally high. Considering this fact, it is obvious that with 2,337,160 farms electrified we still have a long way to go to reach saturation of this group alone. We will not reach it until we have a specialized industry with quantity production. Even on dairy farms where electricity can be used in so many ways it has been shown on an average that only 10 per cent of the connected load is used for farming operations; the other 90 per cent is used for lighting, household appliances, and water pumping. The former figure is exceedingly low when we think of electric service as being used to increase farm production and income.

An electric motor-driven cream separator will finish the job in half the time that hand work will. With the larger sizes of separator it is now a one-man instead of a two-man job.

Records of 25,000 acres in the state of Oregon which were irrigated show a gross increase of $40 per acre over nonirrigated acres. Although irrigation is practiced in every state of the nation, yet today it is only in its infancy in the humid regions.

Individual farmers report savings up to $100 per year by electric feed grinding, but here too we cannot as yet speak of

Table I. Electric Farm Equipment Sold*

	1941	1940	Increase Number	Increase Per Cent
Milking machines	53,463	33,930	19,533	57.6
Cream separators (hand or power operated)	138,785	105,254	33,531	31.8
Milk coolers	13,143	9,134	4,009	43.8
Domestic pumps and water systems	360,412	267,972	92,440	34.5
Brooders	111,689	91,340	20,349	22.3

* Source: MANUFACTURE AND SALE OF FARM EQUIPMENT. Bureau of the Census, United States Department of Commerce.

general use of electric feed grinders on the farm.

Numerous other activities could be quoted where electricity has been used successfully on the farm. But anyone who has had a chance to see the fine research work being done in the various colleges will at once realize that the surface has not yet been scratched and that manufacturers will find a ready market for many items now in the development stage in the colleges, or in the minds, or the cellars of individual inventors. Until recently there was very little incentive for manufacturers to spend money on the development of new devices for the farm. Such new items are hard to sell and they are expensive to sell. Farmers are far apart from each other, and the very sales effort required for introducing a new item on the farm makes the cost of the article in many cases prohibitive. The REA co-operatives use a group-purchase system, and they can arrange with manufacturers to cover a quantity purchase by one agreement. Since November 1939 the Rural Electrification Administration co-operatives purchased most of their meters under the group-purchase plan, totalling about $4,750,000 to the satisfaction of both the consumers and manufacturers. While all the service transformers for new construction have been furnished by the contractor, the co-operatives used the group-purchase plan for securing close to 25,000 transformers for extensions, totalling nearly $2,500,000. Quite a few other items have been handled successfully through the group-purchase plan, making it unnecessary for the manufacturers to try to sell a new item from house to house.

It is important to realize that whatever is manufactured for the farmer must be low in price, rugged, and simple in operation. Only quantity production can bring about such devices.

It is gratifying to be able to report that there is quite a number of progressive American manufacturers who have been giving this matter their careful attention and have been spending considerable sums of money on development of many devices suitable for quantity production.

One manufacturer developed a self-contained quick freezer and cold-storage unit having a capacity of 20 cubic feet, which means that it will hold about 1,000 pounds of meat, or 500 pounds of fruit or vegetables. It is sufficiently small in dimensions to be accommodated on the back porch of the house, and it is estimated that on a quantity production basis it would cost less than $300. There is no question that there is an enormous market for such an item.

Another manufacturer, to make it possible for the farmer to grind his own whole-wheat flour and cereals, making it unnecessary to ship his wheat to the factory and then get it back again in the form of flour, developed a small kitchen-type flour mill weighing only 25 pounds and able to grind 20 to 40 pounds of flour per hour. The cost of this little mill based on quantity production will not exceed $25, and it is hoped that eventually it will have an attachment so that the standard makes of mixer attachments in the kitchen can be attached to the mill, thus eliminating the necessity of buying a mixer. But this idea, along with the freezer, had to be put to sleep temporarily on account of the war.

Dehydration is another very important item. We know, of course, that there is a great deal of commercial dehydration going on in this country and that dehydration made it possible to send enormous quantities of food abroad which otherwise could not have been sent. But two problems on the farm are as yet unsolved.

1. Everyone who has traveled through the country knows that enormous quantities of apples, peaches, and some vegetables go to waste, because the farmer cannot afford to harvest them.

2. It should be made possible for the farmer to dehydrate such fruits and vegetables for his own use.

A small kitchen type dehydrator would solve these problems. One progressive manufacturer developed a small dehydrator weighing less than 100 pounds and being 28 by 20 by 23 inches in dimensions. This little dehydrator does not call for special wiring, because the consumption is so low that it can be attached to the house circuit. It can produce in 12 hours four pounds of dehydrated apples from 25 pounds of apples, and consumes only 2.4 kilowatt-hours per pound of dehydrated apples. This item is likewise kept in abeyance because of the war.

These are outstanding examples of the difference between homemade equipment and manufactured equipment. Numerous types of dehydrators have been designed by various schools, and the designs have been sent to the farmers with the advice that they produce those dehydrators themselves. It is not necessary to dwell upon the fact that during this critical wartime man power on the farm is now very scarce and that the farmer has no time to devote to the production of mechanical equipment, but even in peacetime very few farmers would be interested in producing such a device if a better commercial product were available at a reasonable price. Besides, all of the designs for homemade equipment show electricity consumption much greater than that of the one proposed by the manufacturer. Some of the homemade dehydrators require as much as 5 kilowatt-hours per pound of dried apples as against the 2.4 kilowatt-hours that the manufactured equipment would require. There is no question that there is a market for hundreds of thousands of such small dehydrators if produced in quantity, of high quality, and at a low price. The estimated price of this particular dehydrator is about $36, based on a quantity of 5,000. But other manufacturers likewise are working on the development of a small dehydrator, and no doubt when the market opens up the price will be lower.

The matter of chicken brooders is something for manufacturers as well as for an AIEE committee to study. Here too the number of designs is so great as to be bewildering and make it difficult for a farmer to select the type of brooder he should buy, to say nothing of making one himself. It should be possible now to crystallize a small number of standard designs that will meet all the requirements of the various agricultural schools and individual farmers and prepare some definite specifications under the auspices of the American Standards Association. This and only this will make it possible for reputable manufacturers to go into quantity production of brooders and for the farmers to get the best brooders for the lowest price.

Some studies have been made on soil treatment by application of electricity, and there is a beginning of promising results. In one case, for instance, electric soil treatment eliminated entirely the wild morning glory (bindweed). In another instance a better quality flax was produced after the soil was treated with an electric process. This, too, should be studied by manufacturers with a view toward producing eventually standardized equipment for electric soil treatment. Experiments have been carried on to use electric light for the control of the European corn borer. You know, of course, what a pest the corn borer has been. I have been told that in some parts of the corn belt farmers have stopped raising sweet corn solely on account of the corn borer. These experiments likewise had to stop, because the one who was outstanding in this work is now in the Army, but the work should be picked up again, and standard equipment should be developed that could be sold to every farmer who raises corn.

Some research men have been working on the development of a small pasteurizer

Transmission-Line and System Problems in Supplying Large Electric-Arc Furnaces During Wartime

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ELECTRIC-ARC furnaces are contributing materially to the war effort, and a great number of them, small, medium, and large, have been installed recently, and more are being installed all over the country. These furnaces inherently produce a violently fluctuating load with widely different values in the several phases during the early part of the heat, referred to in the trade as "the meltdown period," and the electric lines and systems to handle such loads must be carefully designed and installed so as to prevent such swinging loads from affecting adversely the system and other critical loads, particularly the lighting, and at the same time to provide sufficient capacity and voltage for satisfactory furnace operation. Also, such lines must be so arranged and connected into the system that the minimum of copper will be utilized—most important now.

This article will discuss some of the basic data necessary to have at hand in the solution of the problem of connecting such loads to an electric transmission system, together with the actual solutions brought about by the installation of 20 furnaces, medium and large, in ten plants of the Duquesne Light Company system.

Briefly, there are five major problems involved, and the last is the most important in time of war:

1. To provide adequate line and/or step-down transformer capacity to carry the heating load that the furnace imposes thereon.

2. Similarly, to provide adequate line capacity to handle the violent one- and three-phase load swings and deliver usable voltage.

3. To connect such line extensions into the system at a suitable location to avoid: (*a*) subjecting other customers to objectionable flicker, and (*b*) causing objectionable operation of various equipment on the system, such as induction-feeder voltage regulators, governing equipment, tie-line controls, rotating equipment, and so forth. (In time of war, such limitations have been stretched of necessity and in some instances waived entirely.)

4. To reinforce the system and/or lines as required.

5. To use the absolute minimum of copper and other critical materials.

Nature of Furnace Loads

The load of a three-phase electric-arc furnace is of a violently fluctuating nature, as is shown in Figure 2, which is the oscillographic record of the amperes in each of the three phases of the 5,000-kva furnace transformer listed as item 7 in Table I. The section at the left was taken during the early meltdown period and, as can be seen, the arc strikes and goes out repeatedly with only a few cycles intermission, and this continues for an appreciable length of time, 20 to 30 minutes or more. The middle section was taken after the furnace had been in operation for a while and was approaching the end of the meltdown period. Note that the arc does not go out so often nor remain out so long, although in this oscillogram the middle phase does fluctuate quite widely. The section at the right was taken during the refining period and is approximately a balanced load, and during this time the arc does not go out.

There is no system problem in carrying the load during the refining period, but there is a very real problem in providing facilities to carry loads of this nature with reasonable expenditures and with minimum use of critical materials.

Figure 3 shows the frequency of instantaneous three-phase reactive megavolt-ampere swings for various-size furnaces, and in several tests made it has been disclosed that the highest swing occurs only once in a six-hour heat, and that the next ten high swings are less than 80 per cent of this one value. This case was on a 10,000-kva furnace transformer, and we have data on larger units which approximately bear this out. The objectionable zones of voltage flicker previously referred to are more likely to be in the latter range than for the one or two infrequent higher swings.

A study of Figure 3 will show the steepness of the curve for the infrequent swings, and the lower swings, or more infrequent ones, will probably not require so stiff a system and hence probably will require less material. We doubt the advisability of tooling up for the one or two maximum swings during a heat of even one or two hours, let alone for the one large swing occurring in a six-hour heat during wartime.

The larger swings produced by several furnaces in simultaneous operation in one plant occur very infrequently—in fact our investigation discloses that they are of less frequent occurrence than the one high swing of one furnace operating alone.

Paper 43-29, recommended by the AIEE committee on power transmission and distribution for presentation at the AIEE national technical meeting, New York, N. Y., January 25–29, 1943. Manuscript submitted November 27, 1942; made available for printing December 21, 1942.

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for the small farm that could be had at low cost, and the prospects are very promising. This work should have the attention of electrical manufacturers. It should be made possible for the small farmer to pasteurize his milk and sell it in the community.

The possibility of a storage-battery-driven electric tractor has not yet been exhausted. Some experiments were made prior to the depression but were dropped. It is difficult to predict whether or not such a tractor would be a success. It is possible that if the cost of energy for charging the batteries at night is very low a market would be opened for small tractors to be used by hundreds of thousands of small farmers.

There is a decided need for simplification of motor starters for medium-sized motors for farm use and also for a reduction in the price of the starters.

When the war is over a great deal will be known about high frequencies that is not known now, and it is possible that high frequencies ranging from audio frequencies to ultrahigh frequencies may have a great many applications on the farm. It may be possible to apply high frequency for soil treatment, and supersonic waves may possibly be applicable for extermination of pests. This subject calls for much more study by electrical engineers.

Reference

1. NEW ENGINEERING AND A NEW INDUSTRY, M. M. Samuels. ELECTRICAL ENGINEERING, volume 61, October 1942, pages 517–20.

Table I. Transmission Supply Lines, Short-Circuit and Voltage Flicker for 20 Furnaces From 1,500 to 15,000 Kva in Ten Plants in Duquesne Light Company System

	Furnace Transformer Data			Supply-Line Data			At Customer's Substation				Volts Regulation on 115-Volt Base and Based on Instantaneously Established Single-Phase Swing		
							At Incoming Bus				At Customer's Substation		
								Reactance in Ohms					
Item No.	Quantity	Largest Transformer (Kva)	Supplemental Reactance Used	Voltage (Kv)	From Network or Separate Line	Figure No.	Three-Phase Short-Circuit (Kva)	Three-Phase	Single-Phase	At High-Voltage Furnace Bus (Reactance in Ohms—Single-Phase)	At Incoming Bus	At Furnace High-Voltage Bus	At Nearest Critical Station Bus
1	3	15,000	No	68	Network	6-*A*	1,179,000	3.925	7.85	24.72*	1.53	4.80	1.37
2	2	10,000	Yes	68	Network	6-*B*	749,000	6.18	12.36	37.02*	2.00	5.98	0.98
3	4	10,000	No	68	Separate**	6-*D*	304,000	15.22	30.44	46.64*	4.92	7.55	1.23
4	1	7,500	Yes	23	Network†	5-*B*	439,000	1.205	2.41	2.41	2.99	2.99	1.97
5	1	7,500	Yes	23	Separate	None	132,000	4.01	8.02	8.02	9.94	9.94	2.75
6	2	6,000	Yes	68	Separate	6-*C*	330,000	14.02	28.04	83.6*	3.52	10.50	1.78
7	1	5,000	Yes	23	Network†	5-*C*	251,000	2.11	4.22	4.22	4.12	4.12	3.74
8	2	4,000	Yes	23	Separate	5-*D*	103,000	5.12	10.24	10.24	8.70	8.70	1.76
9	1	3,000	Yes	23	Network	5-*A*	319,500	1.656	3.31	3.31	2.34	2.34	1.87
10	3	3,000	Yes	11.5	Separate (see text)	4-*A*	364,000	0.363	0.726	0.726	2.02	2.02	1.48

* Includes intermediate transformation between incoming supply lines and the high-voltage furnace bus.

† A separate isolated 22-kv line can be made available by the opening of a remotely controlled oil circuit breaker at the customer's plant if necessary when the furnace loads are swinging.

** A separate line or lines required for line loading and not for furnace load fluctuation.

Pure reactive used for overhead lines and 80 per cent of impedance for cables. All jobs are overhead lines, except item 10 which is all cable.

In specific tests on an installation of four 10,000-kva furnaces in the same plant, the increase of the maximum swings of two furnaces when operating simultaneously melting down over one alone was about 20 to 25 per cent. Similarly for the three similar furnaces over two, the increase was seven to ten per cent additional.

These records of Figure 3 were obtained from high-speed graphic meter charts at three inches or six inches per minute, and it is recognized that there is some error in these charts, because of the fact that the swings take place so rapidly the instrument cannot follow them accurately. The oscillograph is the proper measuring device to use to obtain such records, but it is believed that these curves are indicative of the general relation.

It is also recognized that the lamp flicker, the source of complaints, is the result of single-phase swings, but the single-phase data were not available. However, it is believed that the three-phase data will be indicative of the general relation between single-phase swings of different-size furnaces.

The swings are affected very materially by type of scrap and by how the furnace is operated. For instance, recently we have tested a large furnace which was charged with light scrap loosely packed in, with the resultant swing being about half or two thirds of those on similar furnaces when charged with heavy scrap carefully packed in. Also, the amount of supplemental reactance (the tap connection of the reactor) in use in the furnace transformer has a very material bearing on the magnitude of the swings. Therefore, caution is urged in attempting to use these curves as a criterion of furnace performance, but they will be helpful as a guide.

The number and sizes of furnaces and the conditions of the tests for the curves are given below:

Curve A. Four 25-ton 10,000-kva furnaces and one 36-inch slabbing mill supplied by a 37,500-kva 66/11-kv bank, in normal operation. Details are not available. The mill load is relatively steady and has a small effect on the shape of this curve. New furnaces of this size would probably have a normal rating of 35 tons. Data for curves *B*, *D*, *E*, and *F* were taken on same installation. Curve *A* was plotted from $3^1/_2$ hours high-speed meter record over several days in July 1942 and measured at the 66-kv bus at the power station. The customer is supplied by a 4/0 66-kv line 14 miles long.

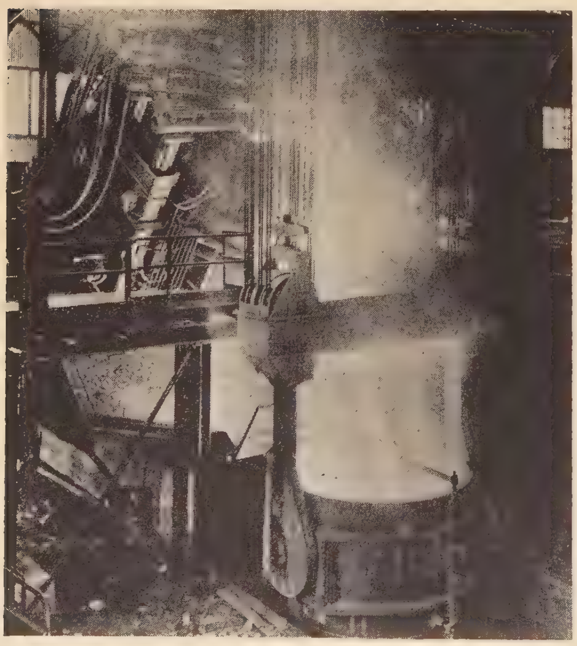
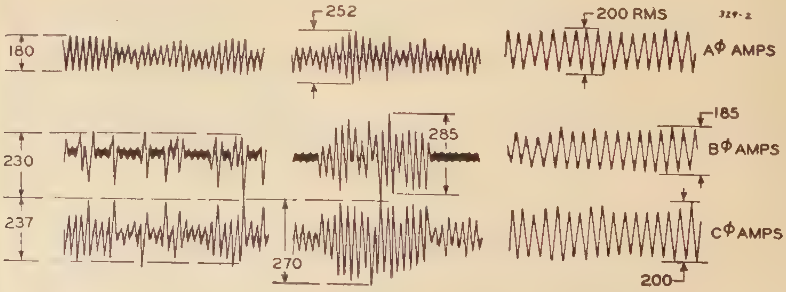


Figure 1 (left). A typical large modern three-phase electric furnace "pouring"



A. Early part of meltdown B. Later during meltdown C. Refining

Figure 2 (below). Oscillograms of the three-phase currents of 5,000-kva furnace during early meltdown, later in the meltdown, and during refining

Note the different degree of stability of the arc

Curve B. One 25-ton 10,000-kva furnace supplied by a 37,500-kva 66/11-kv bank. This is an estimated curve at ten per cent above curve *D*.

Curve C. One 50-ton 12,000-kva furnace supplied at 24 kv by means of a 4/0 line one-half mile long from a very large step-down bank. Curve plotted from five hours ten minutes high-speed meter record for a full heat in January 1940. Heavy scrap used.

Curve D. One 25-ton 10,000-kva furnace supplied by a 9,375-kva 66/11-kv bank. Curve plotted from high-speed charts covering a six-hour run for one full heat in April 1940. Heavy scrap used.

Curve E. Two 25-ton 10,000-kva furnaces, each supplied by one 9,375-kva 66/11-kv bank. Curve plotted from a six-hour high-speed meter record in September 1940, with simultaneous melting down of the two furnaces for $1^3/_4$ hours of this time. Heavy scrap used.

Curve F. Four 25-ton 10,000-kva furnaces supplied by a 37,500-kva 66/11-kv bank with three furnaces melting down simultaneously and one refining. Curve plotted from 40 minute high-speed meter record in September 1941. Other spot tests show infrequent swings higher than these, even higher than for two furnaces, curve *E*. Low values—much higher expected.

Curve G. One 15-ton 7,500-kva furnace supplied by a two-mile 4/0 22-kv line which in turn is supplied from a 60,000-kva 66/22-kv bank. Curve plotted from two hours 15 minutes high-speed meter record of the early part of a heat in April 1942. Light scrap and restricted operation.

Curve H. Two 7.5-ton 6,000-kva furnaces supplied by one 9,375-kva 66/11-kv bank. Curve plotted from six hours 50 minutes high-speed meter record in August 1942. Light scrap and reduced operation.

Voltage Fluctuation Limits of Duquesne Light Company

We have peacetime voltage-fluctuation limits of 1.5 and 2.0 volts on the 22-kv and 66-kv systems respectively for fluctuating loads of arc-furnace characteristics, which we used prior to the war as a guide in connecting furnace loads, but these limits have been waived of course since the war started. The voltage flicker limits are more severe for the 66-kv system in certain zones of frequency of occurrence than for other parts of the system, for the reason that the cost to provide corrective measures would be greater than on other parts of the system. Of course, the same perceptible and objectionable zone will exist, but more consumers would be exposed to the voltage flicker, and hence presumably more complaints would result.

By adhering to these limits before the war, we encountered very little objection to voltage flicker from consumers, and the few have been mainly the result of welder installations which were greatly exceeding these limits, rather than furnace installations. In many cases we now permit fluctuations considerably in excess of these values, even though we realize that they are definitely perceptible and, in certain cases, may become objectionable.

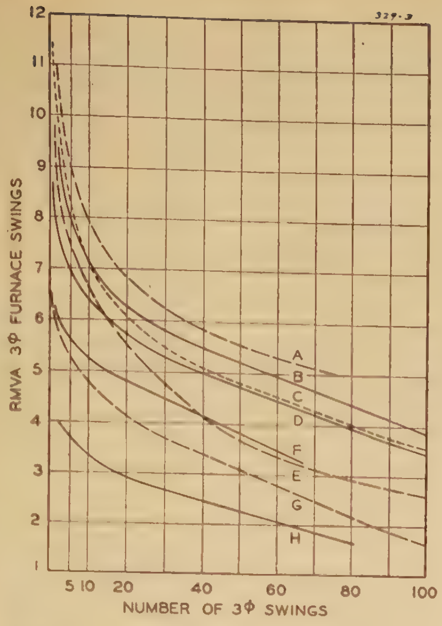


Figure 3. Frequency of instantaneous three-phase reactive-megavolt-ampere swings for various size furnaces, individually and in groups

A = 4–10,000 kva and mill
B = 1–10,000 kva
C = 1–12,000 kva
D = 1–10,000 kva
E = 2–10,000 kva
F = 4–10,000 kva
G = 1– 7,500 kva
H = 2– 6,000 kva

To some, these limits may appear to be rather severe, but our successful experience in applying them, we believe, justifies their retention after the war. We of course exercise judgment based upon experience and stretch these limits in certain cases, even in peacetime. We were considering revising these limits to be more lenient, even before the war, and our experience during wartime will be of considerable use to us in the next revision.

The matter of perception of and objection to voltage flicker is not, in my opinion, subject to exact scientific determination. Much research work has been done on this, and while some consumers will object strenuously to voltage flicker, others would not even be conscious of the same flicker. Some of the best work done on this is covered in a pamphlet entitled "The Visual Perception and Tolerance of Flicker," a report of the subcommittee for project 10, Utilities Co-ordinated Research, Inc., 1937, which, I believe, is available at a reasonable price.

System Regulation

The voltage flicker at the nearest adjacent substation is generally not the governing factor, for this nearest station may be an industrial consumer where considerable flicker can be permitted without encountering objections, whereas a distribution substation supplying lighting may be farther away (electrically) from the furnace than the industrial substation, and yet objectionable flicker may be rendered to the lighting consumers. However, the industrial consumer's substation may be the governing factor, for there may be critical drafting-room lighting requirements or fluorescent lighting in certain processes, or other manufacturing requirements.

The instantaneous swings lasting less than a second (of cycles duration or less) occur more frequently than swings lasting several seconds, the latter reaching the maximum value in several steps, each of which is generally smaller than the maximum swings lasting less than a second. The swings of the shorter duration being more frequent are the final criterion insofar as flicker is concerned, but there is, of course, more total regulation for the swings lasting several seconds, because they reach a higher value.

However, our experience has been, and that of some other power companies bears this out, that the regulation problems of loads lasting several seconds is not of any serious concern, for if you tool up for the instantaneous swings, the condition occurring less frequently causes no complaint.

11-, 22-, and 66-Kv Line Connections for Electric-Arc Furnaces From 1,500 Kva to 15,000 Kva

Table I shows data on the transmission supply lines, short-circuit kilovolt-amperes, and voltage flicker or regulation for 20 furnaces from 1,500 kva to 15,000 kva in ten plants on the Duquesne Light Company system, and there is also included the impedance in ohms, both one-phase and three-phase, at apropos locations.

In three instances, the peacetime limitations are or will be exceeded, and from none of these are complaints being encountered, nor are any expected. Also, it is believed that no serious complaints

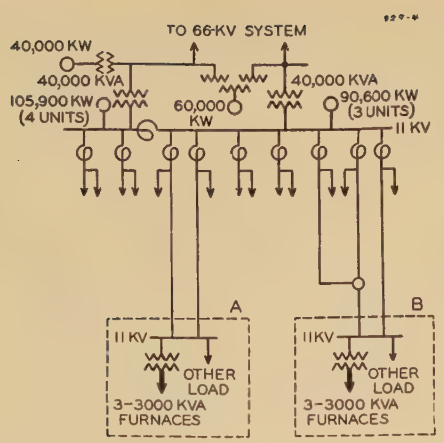


Figure 4 (left). Eleven-kilovolt line connections for electric-arc furnaces of 3,000 kva

Larger furnaces can be supplied by the methods shown in the *A* diagram of this figure, with of course the supply lines being reinforced to carry the heating load of the furnace

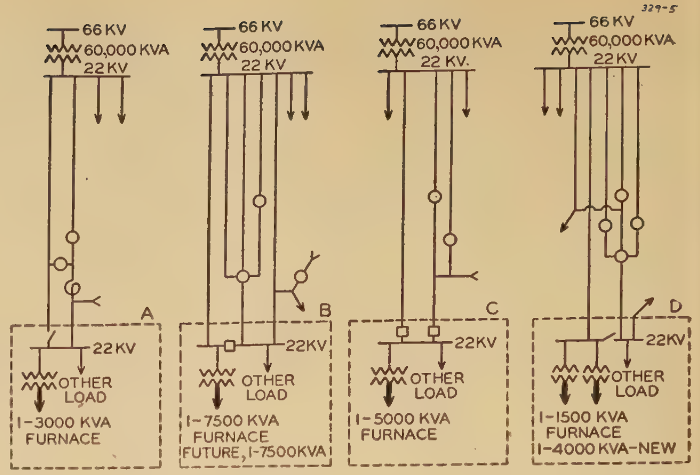


Figure 5. Twenty-two-kilovolt line connections for electric-arc furnaces of 1,500 kva to 7,500 kva

Sixty-six-kilovolt transmission system with 624,000-kw generating capacity

during wartime will be encountered from flickers considerably in excess of those shown. Further, it is felt that values appreciably in excess of those shown would not interfere with war production.

Item 7 in Table I will be much less severe when isolated temporarily as described in the note beneath the table.

11-Kv Line Connections for 3,000-Kva Electric-Arc Furnaces

Figure 4 shows two methods of 11-kv line connections for three 3,000-kva furnaces, and the data for sketch *A* of this figure are given in Table I as item 10. The two lines of 500,000-circular-mil cable, 1.5 miles long, feeding this furnace substation, connect to separate busses at the power station which are supplied through reactors from the main generating source, and the feeder bus also supplies other distribution substations as indicated by the short lines with arrows. The voltage flicker of 1.48 volts at one of these two substations is the limiting point and is less at the 11-kv feeder bus in the power station, that is, 0.44 volt.

The wartime cable rearrangement involved a very small amount of copper.

The corresponding voltage regulation at the power-station feeder bus for sketch *B* of this figure is 0.44 volt and is 1.72 volts at the nearest industrial consumer.

At the time of preparing this paper, one furnace only is in service.

22-Kv Line Connections for 1,500-Kva to 7,500-Kva Electric-Arc Furnaces

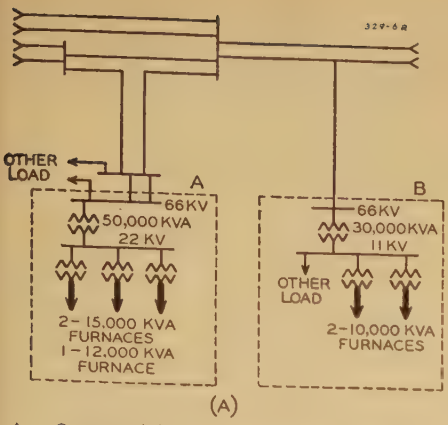
The larger number of our furnaces are connected to the 22-kv transmission network by the lines in that vicinity rather than by an exclusive line from a major transmission stepdown substation. Some of them, however, are connected to a separate exclusive line, items 5 and 8 in Table I being in this category.

Figure 5 shows several recent installations. Diagram *A* of this figure covers the supply to a 3,000-kva furnace and is item 9 in Table I, and the voltage drop at the nearest critical substation bus is 1.87 volts. It will be noted that there is another line normally open to this consumer, and, when one is operating on this second line with the first open, the voltage drop at the nearest critical substation bus will be somewhat greater. The 22-kv lines are 4/0, and the wartime line rearrangements and the waiving of peacetime flicker limits resulted in installing only 800 feet of 4/0 line.

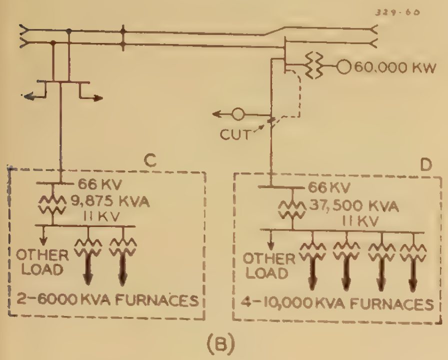
Diagram *B* of Figure 5 covers the supply to one 7,500-kva furnace now in service and another one authorized and is item 4 in Table I. The drop at the nearest critical substation is 1.97 volts. There are other substation busses getting larger voltage drops than this, but they are industrial consumers supplied at 22 kv and are not so critical. The exclusive line is 4/0 and is two miles long. Careful study developed a scheme of handling this furnace load, without any line copper being used, by rearranging the consumer's 22-kv bus in order to isolate the direct line at times of meltdown.

Diagram *C* of Figure 5 covers the supply to a 5,000-kva furnace which has been in service for a number of years and which creates much wider fluctuations than other furnaces of this size and larger. This is item 7 in Table I. The voltage drop on the basis of normal single-phase swings expected from such a size transformer at the nearest critical substation bus is 3.74 volts, and at the 22-kv bus of the nearest major-ring stepdown substation is 2.17 volts. With the abnormal swings that this furnace transformer produces at times because of its unusual impedance characteristics, these values are exceeded, and certain remedial steps were taken shortly after this furnace was installed. These remedial measures consisted of changing the adjustment in voltage-regulator relays on the induction feeder regulators in the nearby substations to permit the higher short-time voltage swings to filter through the relay without causing the regulator to operate, and placing a little time delay in the initiating devices of these induction feeder regulators; also, certain 22-kv line rearrangements were made in this vicinity at that time, and remote control facilities were added to the incoming line breakers at this substation, so that one of them could be opened and the furnace operated on a direct 4/0 line approximately three miles long from the stepdown transmission substation. These remedial measures have eliminated completely the complaints that we received in copious quantities when this furnace was first placed in service. Actually, the consumer is now operating radially on one direct line from the major stepdown transmission substation during the meltdown periods.

Diagram *D* of Figure 5 covers the supply to an old 1,500-kva furnace and a new 4,000-kva furnace. The latter furnace required a separate radial line. The final line arrangement in the district is shown. Pertinent electrical data are covered in item 8, Table I. The old furnace and the mill load were supplied by the 22-kv network in that vicinity, and the old furnace did cause an undesirable but not intolerable voltage condition. For this reason it was reconnected on the new radial line together with the new 4,000-kva furnace. With the furnaces supplied by an exclusive line (1/0 copper and six miles long), the voltage drop at the nearest critical substation bus is 1.76 volts. A bus tie switch is installed between the furnace bus and the bus which is supplied from the network. This was done in order to permit the furnaces to be operated at reduced voltage in case the radial line is out of service for an extended



A. Sixty-six-kilovolt transmission system with 624,000-kw generating capacity



B. Sixty-six-kilovolt transmission system with 624,000-kw generating capacity

Figure 6. Sixty-six-kilovolt line connections for electric-arc furnaces of 6,000 kva to 15,000 kva

period. If normal furnace voltages are used under this condition, the voltage fluctuation at the nearest critical bus in the network is intolerable.

A *new* exclusive furnace line would have required a large amount of critical materials. By consolidating loads in two districts into one district, a 22-kv line was released and extended for service to the furnaces. Actually, 53,000 pounds of copper were saved by the rearrangements as compared with a new exclusive line.

66-Kv Line Connections for 6,000-Kva to 15,000-Kva Electric-Arc Furnaces

The 66-kv method of supply is used for four plants, one of them having the largest furnaces on our system. Figure 6 shows these connections.

Diagram *A* covers the supply for three large furnaces—two 15,000-kva and one 12,000-kva. This is item 1 in Table I. One furnace is in service now, and the others are expected to be in service during the winter of 1942–43. This is an interesting connection, for the mill where the furnaces are located is supplied at 66 kv from a power-company substation approximately 1.0 mile away. A considerable portion of other load of this same consumer is supplied from the power-company bus and from the 66-kv furnace bus in the mill where the furnaces are located, all as shown in this diagram. As a matter of interest there are 60-cycle generators proposed for connection to the consumer's 66-kv lines in one of his mills, and we will operate in parallel with these generators which are scheduled for service sometime in 1943. The two power company 4/0 66-kv lines 0.5 mile in length were installed four years ago for mill load only then, and today for furnaces alone we would supply one line. The voltage drop at the nearest critical substation is estimated to be 1.37 volts.

Diagram *B* covers the supply for two 10,000-kva furnaces which are expected to be in service early in 1943. This is item 2 in Table I. The "other load" shown in this diagram is a very small part of the load and is merely for handling facilities and shop lighting, and this is purely an electric-furnace plant for producing steel ingots and will in no way fabricate or process them. This was a fortunate location as the 1/0 line extension from the main 66-kv transmission will be only approximately 1.5 miles. The voltage drop at the nearest critical substation is estimated to be 0.98 volt.

Diagram *C* covers the supply for two 6,000-kva furnaces. One of these was in service in 1941, and the other early in 1942. This is item 6 in Table I. This location was not so fortunate since it required 4.0 miles of 4/0 66-kv line added to 4.0 miles of existing 4/0 line from the transmission substation bus to reach this. The voltage drop at the nearest critical substation is 1.78 volts.

Diagram *D* is an interesting installation and supplies four 10,000-kva furnaces over a 4/0 line about 14 miles long. This is item 3 in Table I. This furnace load began with one 10,000-kva furnace connected at the point marked "cut" and supplied from the transmission network in this vicinity by a nine-mile line extension. Within five months a second furnace was installed and was carried without any line reinforcing. Then, about a year later two more 10,000-kva furnaces were installed, and this necessitated cutting the line, at the point shown in diagram *D*, from the network and extending it back to a stiffer source, which is now a generating-station bus but which at that time was at a point on the 66-kv transmission. The voltage drop at the power station 66-kv bus for the present connection is 1.23 volts. At the time of the initial installation the voltage drop at the nearest substation was higher, but no complaints were received.

Kilowatt Demand and Kilowatt-Hours

Table II gives these data for various sizes of furnaces, which data are incomplete, for in some cases the kilowatts and kilowatt-hours are totaled along with other mill load, and the remainder are on other systems, and only partial data are available to the writer. Experience and skill, together with other factors listed below, will affect the electrical performance very materially, as well as the tonnage output.

Production of steel by electric melting, and the electrical requirements—kilowatts, kilowatt-hours, and load swings—are governed by many variable factors, some of which are subject to some degree of control; among these important factors are the following:

Table II. Furnace Size, Kilowatt Demand- and Kilowatt-Hours Per Ton

Partial List for Representative Sizes

Item No. in Table I	Furnace Size Net Short Tons	Transformer Rating—40 C (Kilovolt-Amperes)	Kilowatt-Hours Per Net Short Ton Produced	Kilowatt Demand 15-Minute Integration
*		1,500		1,350
10*	3.0	2,500		2,300
9*		3,000		2,200
	6.0		827	
8*		4,000		3,630
	10.0		795	
6	10.0	6,000	604	6,240
4	7.5	7,500	608	7,500
	10		723	
3	25	10,000	580	8,400
	25		569	
	30		601	
	35		585	
	35		569	
	50	12,000	523	
	60		556	

* Foundries. All data not available—see text.

1. Type and size of scrap.
2. How charged—"packed in," and so forth.
3. Quality of finished production—mild steel, alloy, high grade alloy, and so forth.
4. Skill of furnace crew, particularly that of the turn foreman and melting superintendent.
5. Proper size of transformer.
6. Use of proper transformer taps and control adjustment.
7. Design of entire electric installation—furnace, transformer, wiring, and control, the latter contributing very largely to the success of larger arc furnaces.
8. Charging equipment and general layout.

The nominal tonnage rating of an electric-arc furnace used herein does not indicate necessarily the actual production capacity, for, as previously explained many variable factors affect the production, and this is merely a nominal rating for identification purposes, and actually the tonnage produced may be more. Usually, with the larger furnaces for steel making, the nominal tonnage rating, so-called, indicates the holding capacity of the furnace in net tons, whereas with some of the smaller furnaces in foundries the rating is the net ton produced per hour. One large manufacturer has within the last few years ceased to give his furnaces a rating and now identifies them by a type number.

The experience of a considerable number of plants using electric furnaces over a number of years has disclosed that in some installations the furnace transformer was replaced with a larger transformer, since we had had several such cases on our system—some within the last year. This comes about for such reasons as enlarging the furnace, more tonnage requirements, melting different materials, becoming more skilled in the operation, and so forth.

Other Power Companies' Fluctuation

Different power companies have different requirements for the connection of furnace loads. All of the power companies on which I have data indicate that the frequency of the furnace swings and their magnitude govern the amount of stiffness required in the system at the point of service, for these are the criteria of the flicker which will result in voltage complaints if any are incurred. For instance, one power company requires a short-circuit duty at that spot of 1,000,000 kva for connecting a 10,000-kva electric-arc furnace on the basis of limiting the voltage fluctuation to two per cent, assuming the instantaneous swings to be twice the transformer rating. Another power company feels that the short-circuit duty should be 750,000 kva or greater on the basis of a two-volt flicker on a 120-volt base. Some other power companies have installations in satisfactory operation with five to six per cent voltage swings on the consumer's primary bus, and they feel that 7.5 per cent would also be satisfactory, and they make reference to two per cent limits on the system at critical points.

One large power company claims that its test data disclose single-phase swings from approximately 1,500 kva to 6,500 kva for three-phase electric-arc-furnace transformers varying in size from 1,000 kva to 10,000 kva, with corresponding power-factor variations from approximately 40 to 65 per cent. The Duquesne Light Company test data for several comparable installations check fairly closely with these figures.

All and sundry advise caution in supplying lighting, particularly fluorescent X-ray equipment and other critical apparatus from consumer's primary busses and from lines and/or busses that supply such critical load.

One large power company is fortunate in having most of its heavy industrial load confined to a definite area and has provided a separate bus known as "flicker bus," from which all such loads are supplied. Such a bus can stand much greater voltage fluctuations than one carrying lighting and other critical load. Not many are so fortunately situated.

Early Furnace Experience

Duquesne Light Company has been supplying electric-furnace loads since 1908, and, it is thought, it was the first power company to pick up such load. Even in those days the necessary research work and calculations were made before connecting a furnace to the system, and a trial installation was actually made of the first furnace in the power-station yard, with one engine-driven generator isolated to carry the load for the tests. Subsequently, as a result of these tests, the furnace installation was made in the steel mill and was supplied for a while by an isolated generator and line. As the system grew, the generator and line were tied into the network. This installation was in use in this plant for many years and was scrapped just a few years ago, but other larger furnaces were later installed and have been in use for many years.

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Insulation Testing of Electric Windings

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I. Introduction

THE paper describes a new method for the testing of insulation of windings, the advantages of which are adequate stressing of turn and conductor insulation and certain detection of defects. The new tester consists of a repeating-type surge generator, cathode-ray oscillograph, and synchronously driven switching equipment. The surge generator provides a succession of voltage waves of controlled shape adjusted to give the necessary turn and conductor insulation stresses. The cathode-ray oscillograph is timed to give a stationary image of the repeating surge on the screen. By virtue of the special method of surge application and oscillograph connection, the waves on the screen indicate turn or conductor-insulation defects quickly and certainly.

In high-potential testing, voltages in all parts of a winding should be high enough to result in insulation failure at all points where insulation strength is not adequate to withstand service conditions. To this end, test voltages must be used sufficiently above normal excitation voltages to properly stress conductor insulation and sufficiently rapid in rate of change to stress turn insulation properly. Accordingly, power-frequency voltages, usually two times normal plus 1,000, are used for conductor-to-ground insulation and high-frequency voltages applied directly or by induction are used for turn insulation.

In service, insulation must withstand normal voltage excitation, switching voltages, and other transients such as lightning.[1] Switching surges range below 5.5 times normal but may, as in the case of lightning transients, have high rate-of-change characteristics. Time to crest may be as low as one microsecond. As nearly as practical these service voltage conditions should be reproduced in test.[2–13] The test method described in this paper is directed toward the ends of adequate stressing of all insulation and certain detection of weak spots.

The new method has been used for different types of coils and windings and has been applied to particular advantage in the testing of completed windings of three-phase low-voltage rotating machines. In this case surge voltages are applied alternately to each of two of the terminals of the machine with the oscillograph connected to the third terminal.

II. Wave Propagation in Machine Windings

Experimental work and practical testing experience on production machine windings have indicated definite improvements in line with the objectives already stated. As very little experimental work has been reported on wave propagation in windings, it seems advisable to include here a few oscillographic records of voltage wave conditions pertinent to the surge performance of machine windings.

A most significant characteristic of these windings from the standpoint of surge mechanism is that capacitance and inductance elements are necessarily well distributed as to physical position and, therefore, have very low over-all capacitive or inductive coupling. In this report the machine winding is the opposite of the transformer where over-all capacitive and inductive couplings are very high. Because of this distribution of capacitance and inductance elements, the electrical mechanism of the machine winding is that of the traveling-wave as contrasted with the standing-wave mechanism of the closely coupled transformer winding. The surge performance of a machine winding is illustrated in Figure 1 which shows voltage wave shapes measured at suitably located tap points along one leg of a three-phase winding of a large rotating machine. The voltage wave applied was unidirectional in polarity with a time to crest of one microsecond and to half-crest on the wave tail of 5.6 microseconds.

In Figure 1 in the left vertical column are shown the tap voltages for the case of the neutral end open. The voltage wave shape applied is at the top with consecutive taps toward the open end in order below. The unidirectional polarity impulse traveling through the winding sets up a standing-wave oscillation at the open end which has a frequency of some 34,000 cycles per second. Voltage amplitude along the winding first decreases, then increases, reaching a value some 20 per cent above that of the applied wave at the line end. The low-amplitude oscillation at the beginning of the neutral end wave is the result of direct coupling across the winding, as soon as the applied wave enters the winding at the line end. The winding time length, as indicated by this displacement, is just under 4.0 microseconds.

In Figure 1, center vertical column, are shown tap voltages for the case of the neutral grounded, through a resistance of 200 ohms equal to the experimentally determined surge impedance. This series of oscillograms shows the voltage wave traveling through the winding without major reflections and with appreciable amplitude attenuation so that the neutral end wave is about 60 per cent of the applied wave. This attenuation is accompanied by some distortion resulting in a slight lengthening of the wave tail. The loss in voltage amplitude is mainly on the sharp front of the wave, and this has the effect of increasing the time to crest from one microsecond at the line end to five microseconds at the neutral end.

In Figure 1, right vertical column, are shown tap voltages for the case of the neutral end directly grounded. In this case the polarity of the reflected voltage wave is reversed, and a frequency of 77,000 cycles or something more than twice the open-ended winding frequency is obtained. The voltage amplitude decreases at each tap point and at the neutral end is, of course, zero across the ground connection.

These oscillograms show the nature of the voltage variations in a rotating machine winding under conditions of applied surges such as are obtainable with switching, lightning, or other circuit variations. Further experimental work has shown that steeper and short-duration waves are attenuated more rapidly, and more sloping and longer-time waves less rapidly. This type of information can be used as a basis for consideration of voltage stresses and detection of weak spots in winding-insulation testing.

III. Winding-Insulation Testing Method

Experimental work was conducted on improvements in winding-insulation testing, particularly as regards across-winding stresses and weak-spot detection in view of the surge mechanism already described. As a result of this work, the circuit arrangement and method of connection derived from the oscillograph electric transient analyzer[2] and shown in Figure

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C. M. Foust and N. Rohats are both with the general engineering laboratory, General Electric Company, Schenectady, N. Y.

2 was devised. The principal elements involved are the repeating surge generator at the bottom left, a cathode-ray oscillograph at the top right, the time-co-ordinating and beam time-axis circuit at the top left, and a synchronous reversing switch shown at the bottom right, along with a typical wye-connected winding of rotating machine. The surge generator consists of supply transformer 10, charging tube 11, main capacitor 6, and discharge resistance and tube 7 and 5. This circuit provides a capacitor-discharge voltage wave which is applied through a synchronously operated reversing switch 9 to the machine windings 12 and 13 under test. Time-co-ordinating circuit 1, 2, 3, and 4 supplies time-axis voltage for the cathode-ray oscillograph and operates the surge generator once for each cycle of power-supply frequency, which results in a stationary image of the fault-indicating wave on the oscillograph screen. For practical testing purposes, this equipment is mounted in a cabinet as shown in Figure 3 with the oscillograph at the top and all controls below.

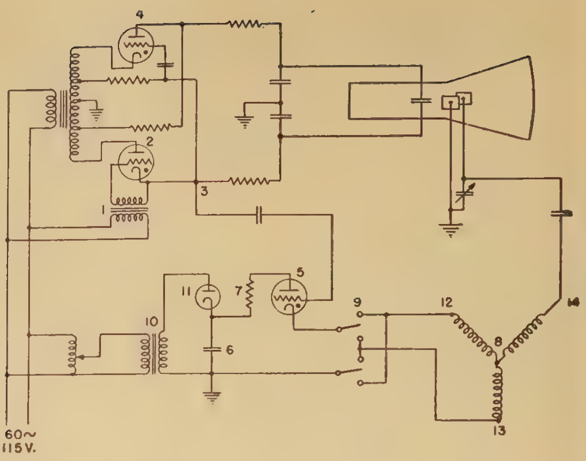


Figure 2. Schematic diagram of winding-insulation tester

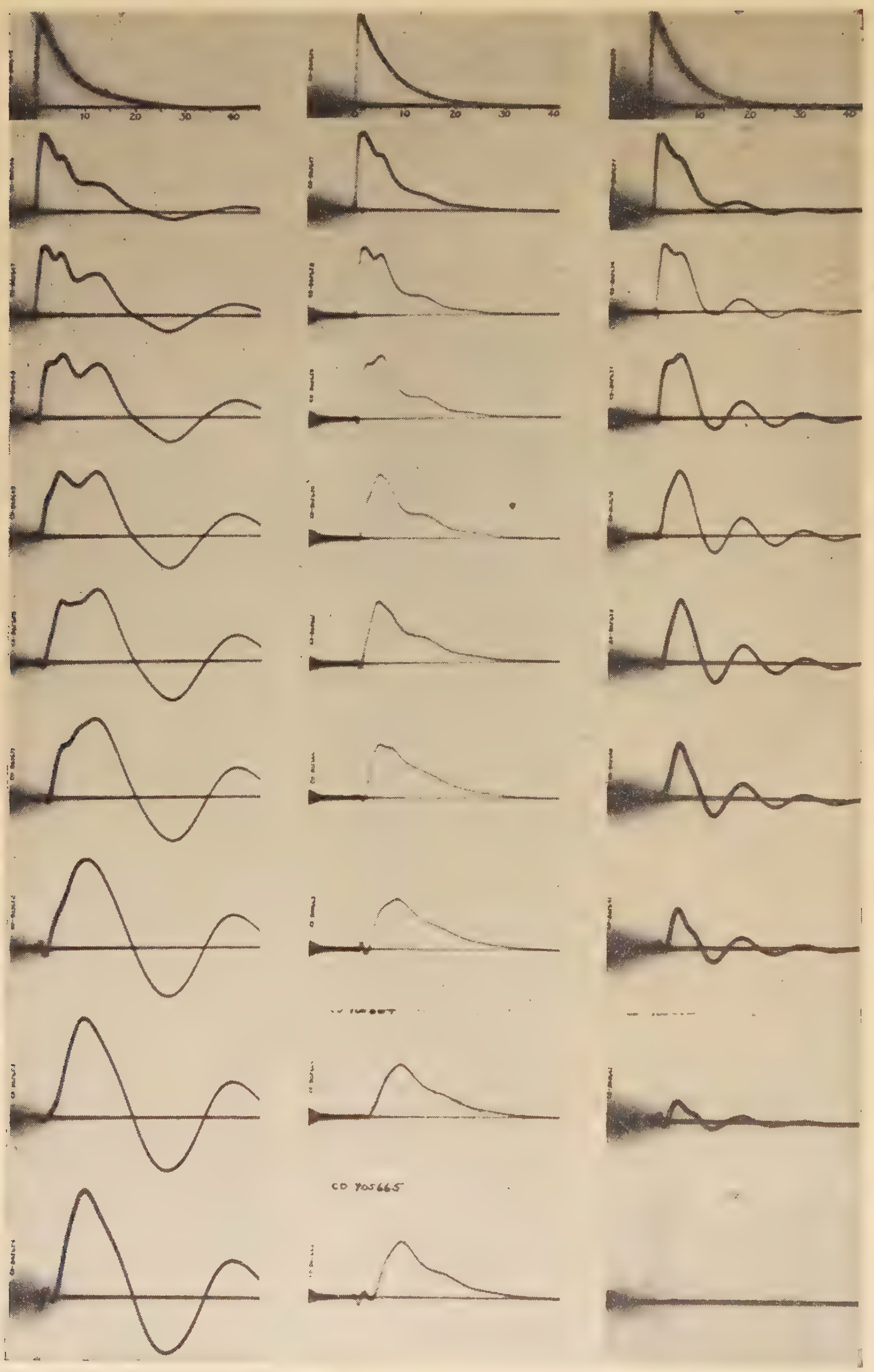


Figure 1. Wave propagation in machine windings

Applied line surge voltage at top. Neutral surge voltages at bottom

Left—Terminus open

Center—Terminus grounded through surge impedance

Right—Terminus grounded directly

In detail the whole circuit operates as described in the following paragraphs.

The peaking transformer 1 trips the sweep thyratron 2 when its plate is on the crest of the positive half-cycle, thereby

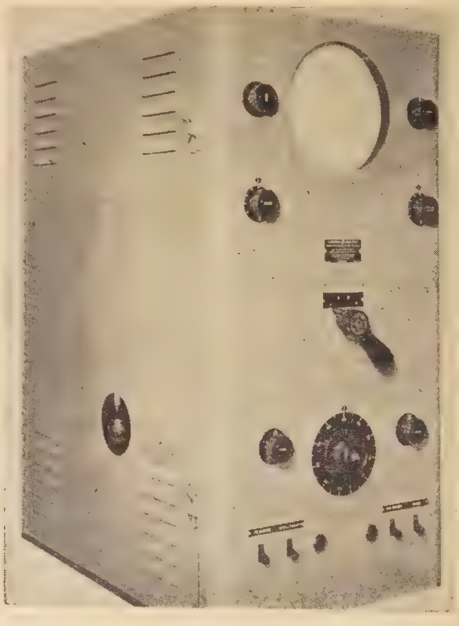
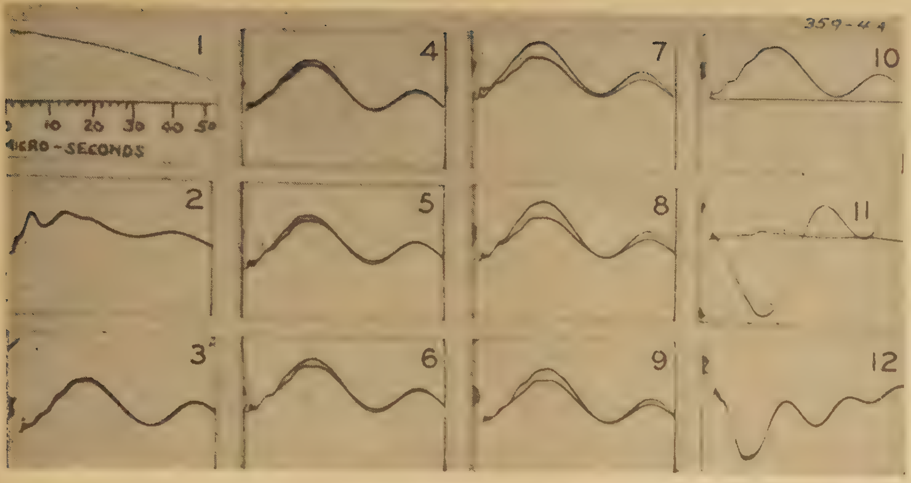
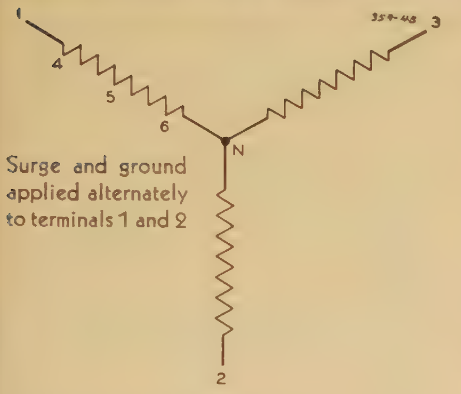


Figure 3. Winding-insulation tester, 10 kv





Surge and ground applied alternately to terminals 1 and 2

Record	Fault	Location	Oscillograph
1	None		1
2	None		N
3	None		3
4	One turn short circuit	4	3
5	One turn short circuit	5	3
6	One turn short circuit	6	3
7	One coil short circuit	4	3
8	One coil short circuit	5	3
9	One coil short circuit	6	3
10	Ground	1	3
11	Ground	5	3
12	Ground	N	3

Figure 4. Winding-insulation tester records, single winding

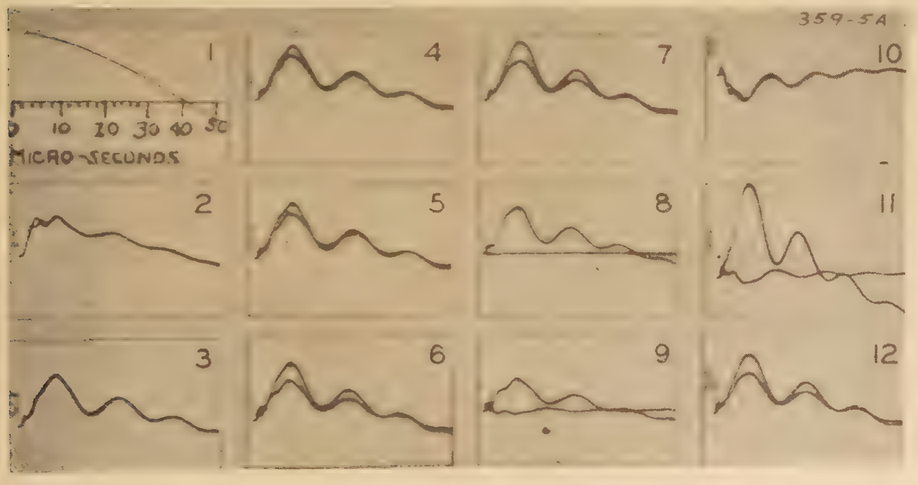
applying a positive impulse to point 3. This positive impulse, in addition to providing one-half the sweep voltage, also trips the tube 4 (which gives the negative half of the sweep), and the large thyratron 5. Tripping of 5 discharges capacitance 6 through resistance 7 into the test winding 8 by way of the synchronous reversing switch 9. Capacitance 6 was charged on the previous half-cycle from transformer 10 through rectifier 11.

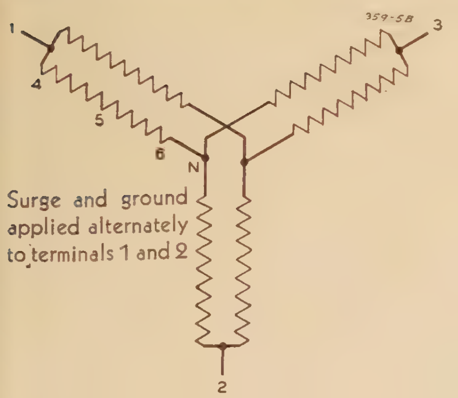
The synchronous switch acts to send alternate surges in opposite directions through the winding 8 between terminals 12 and 13. Terminal 14 is connected through a capacitance voltage divider to the oscillograph deflection plates. The wave shape shown by the oscillograph is, therefore, a resultant of the voltage at the neutral and the voltage induced in the phase to which it is connected.

If the winding is perfectly symmetrical, the wave at the neutral and on the oscillograph will be the same for either direction of surge travel, so that a single standing wave will be shown on the oscillograph screen. If the symmetry is disturbed by a shorted turn or coil, in legs 12 or 13, a ground or improper connection, the two directions of surge travel will give different results and, consequently, two waves of different shape on the screen. Experience has shown that this connection arrangement is extremely sensitive to winding defects and that, to a considerable extent, the relative shapes and amplitudes of the two waves can be used to indicate the nature of the defect.

IV. Specimen Winding-Insulation Tester Records

The oscillograms of Figure 4 show winding-insulation tester performance on a three-phase rotating machine winding. The tester circuit was arranged to give a voltage wave of (1 x 40) microsecond shape. This wave, shown at 1, was recorded by connecting the oscillograph deflection circuit to the impulse input terminal 1 in the diagram. Oscillogram 2 is the wave shape at the neutral of the motor winding obtained by connecting the oscillograph deflection at the motor neutral or point *N* on the diagram. It will be noted that the wave front at the neutral is considerably more sloping than the applied wave of 1, requiring some six microseconds to crest value. Only one trace is shown, indicating identical wave shapes, whether the wave is applied at 1 with 2 grounded, or applied at 2 with 1 grounded. Oscillogram 3 of Figure 4 shows the wave shape at the end of winding 3 with the characteristic standing-wave voltage oscillation agreeing with oscillogram of Figure 1 at the bottom of the left-hand column. Oscillogram 3 also shows only one wave, thereby indicating symmetrical conditions in phases 1 and 2. For oscillograms 4 through 12, the oscillo-





Surge and ground applied alternately to terminals 1 and 2

Record	Fault	Location	Oscillograph
1	None		1
2	None		N
3	None		3
4	One turn short circuit	4	3
5	One turn short circuit	6	3
6	One coil short circuit	4	3
7	One coil short circuit	6	3
8	Ground	1	3
9	Ground	5	3
10	Ground	N	3
11	Reversed phase	1 to N	3
12	Reversed coil	N	3

Figure 5. Winding-insulation tester records, double winding

graph deflection is connected to terminal 3, which is normal for production-line testing of windings.

To illustrate the detection of winding faults, turn and coil short circuits and grounds were made at several points in leg 1 and oscillograms taken of the standing waves on the oscillograph screen. Oscillograms 4, 5 and 6 show the two wave traces produced by one-turn short circuits at different points in leg 1, as indicated on the diagram at 4, 5, and 6.

The two waves in each case are offset from each other sufficiently to constitute an unmistakable indication of the winding faults applied. In the same manner oscillograms 7, 8, and 9 show the offset waves obtained with a single-coil short circuit the differences in waves in each case being greater than for the single-turn short circuit. By stopping the synchronous motor and sending all impulses into terminal 1, and viewing the wave, and then into terminal 2 and viewing the wave, it has been determined experimentally that these waves are above and below the no-fault wave of oscillogram 3.

Oscillograms 10, 11, and 12 of Figure 4 show wave shapes corresponding to applied line-to-ground insulation faults at points 1, 5, and *N* of the motor winding. Oscillogram 10 taken with the ground on terminal 1, of course, shows a zero deflection line for the wave applied to that terminal. The single wave of 10 produced by the surge entering terminal 2, corresponds to the double wave of oscillogram 3.

Oscillogram 11 also shows one low-amplitude wave corresponding to the impulse applied at 1 with point 5 grounded and a high-amplitude wave corresponding to the impulse applied at terminal 2. For oscillogram 12 the neutral was grounded, and the waves are superimposed because of the symmetry of this connection arrangement.

These oscillograms and those of Figure 5 which show wave shapes for a double-circuit wye connection illustrate the wide range of surge-voltage variations corresponding to possible insulation faults and connections. A detailed treatment of these is not possible within the scope of this paper. However, stated generally, these wave shapes are determined by the propagation characteristics of the impulse and the magnitudes and directions of induced voltages in the leg connected to the oscillograph. The significance of all this in insulation testing of windings is to be found in the clear-cut manner in which insulation and connection faults are disclosed by the two-way method of surge application and the standing waves on the cathode-ray oscillograph screen.

V. Conclusions

1. A new winding-insulation testing method for completed machine windings has been described.

2. The new method applies suitably controlled voltage wave shapes to give the required turn and conductor insulation stresses for adequate testing practice.

3. The new method provides for immediate detection of insulation faults and winding dissymmetries.

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TRANSACTIONS SECTION

Preprint of Corresponding Pages From the Current Annual AIEE Transactions Volume

Any discussion of these papers will appear in the June 1943 Supplement to Electrical Engineering—Transactions Section

Generation of Electric Charges by Moving Rubber-Tired Vehicles

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Synopsis: The current flowing to ground from an automobile, whose rear wheels were turning on a dynamometer, was measured. The magnitude of the constant current measured, in the order of microamperes, shows that the vehicle is electrically charged by conduction through the tires. The experimental results are consistent with the conception of a simple equivalent circuit. These results were checked by road tests. Calculation shows that the tires are usually conducting sufficiently to discharge a car in a short time after it has stopped moving.

COMMON experience demonstrates that a rubber-tired vehicle in motion often accumulates a charge of electricity of sufficient magnitude to shock a person who touches the vehicle immediately after it comes to rest.

It is known that this electric charge is produced when the rubber tires contact pavement and then separate from it. This phenomenon has recently been studied by Beach,[1] Cadwell, Handel, and Benson[2] and by Liska and Hanson.[3] These investigators studied the potentials developed and potential distributions around the tire. Most of our experiments were confined to the measurement of currents generated, rather than voltages.

Theoretically there are two methods by which a vehicle can acquire a potential above ground caused by tire static. One method is by induction from the charges produced on the rubber tires. The other method is by the conduction of this charge through the rubber tires to the body of the vehicle. We have measured continuous currents, as high as four microamperes, flowing to a grounded conductor from a car, under normal driving conditions. This continuous current can be produced only by conduction through the tires. It is well known that any charges produced on the body of the car by induction from the charges on the tire, will produce only a *momentary* current when the vehicle is grounded, and cannot possibly maintain a steady current to ground.

Inductive effects due to the existence of charges on the surface of the tires can produce the distribution of charges on the body of the vehicle that is necessary to maintain the body at one potential. The potential, however, is determined entirely by current flowing through a resistance.

Experimental Results

It is difficult to make accurate measurements of currents generated by moving vehicles on the road. Accordingly, in the attempt to determine the currents generated, the first experiments were conducted on an engine-testing dynamometer stand. This device is essentially a large Prony brake. The rear wheels of the automobile rest on a large metal drum that is free to revolve at a rate corresponding to any desired road speed.

In making measurements of currents generated, the body of the car was grounded through a microammeter, the currents being large enough to come within the range of an ordinary instrument.

The rubber tires of a moving vehicle in contact with the pavement produce a difference of potential between the rubber and pavement, and a separation of electric charges occurs. The tire always acquires a negative charge. As the tires move from the contact position on the pavement, or on the dynamometer, the potential of the charges must increase. As the potential increases, the charges seek a path to ground. Our experiments have led us to the conclusion that two paths to ground are followed:

1. Directly across the tires to the wheel hub and the car body. and from the car body to ground through the ammeter.
2. Back along the surface of the tire to the grounded dynamometer, or, in the case of a moving vehicle, to the ground plane existing under the pavement.

Thus, the equivalent circuit of a car on a dynamometer stand may be represented by Figure 1. The current I generated at the tire tread has two paths to ground:

1. To the wheel through the resistance of the tires, R_w, to the car body and through the ammeter to ground.
2. Back to ground across the surface of the tires, through a shunt resistance, R_g.

Figure 2 is a typical curve showing the measured current plotted against speed of the rear wheels. This current flows from the body of the car to ground when the rear wheels revolve on the drum of the dynamometer. The data were taken with a 1941 Chevrolet having the standard 6:00–16 tires. Various pressures were used, and a wide range of tractive loads was applied. In general, we found only a small increase in current with a considerable increase in load and only a small increase in the current with a large decrease in tire pressure. These results indicate that the charges produced are not determined primarily by the amount of

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The experimental work was financed by a grant from the American Petroleum Institute for a study of the production of electric charges in handling petroleum products, and the results described in this paper constitute a part of that program. The authors acknowledge the assistance of Harold Albin, Pasadena, Calif., who made available the use of his dynamometer for this work.

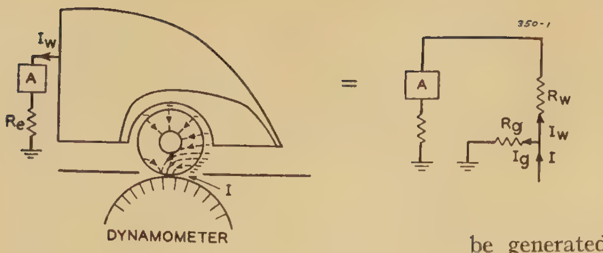

Figure 1. Equivalent circuit of a car on a dynamometer stand

friction, but rather by the speed and separation of contact.

The curve of Figure 2 is not a straight line but shows some decrease in the rate of production of the measured current at higher speeds. We believe that the total charge produced at the surface of the tire is directly proportional to speed at all the speeds indicated and that this falling off of current measured at high speeds is due to changes in the equivalent resistances, R_g and R_w, shown in Figure 1.

With increased speed it is to be expected that the tire temperature will increase. Rubber, as compounded and used in tires, has a negative temperature-resistance coefficient. Therefore, the tread and the surface of the tire near the tread, due to its higher temperature, will decrease in resistance more than the main body of the tires. Since most of R_g resides in the tread and in the portions of the tire near the tread, R_g decreases more than R_w as the speed increases. A greater portion of the total current generated thus goes to ground through the shunt path, and the readings of the ammeter flatten out.

Figure 3 indicates that an increase in temperature will change the ratio of resistance R_w to R_g. This curve shows the current measured at a constant speed, plotted against time. Before this run, the tires were allowed to cool. There is a decrease in measured current for about a minute, and then a substantially constant value is reached. This manner of change of current measured is explained by assuming that the ratio of R_g to R_w decreases as the temperature of the tires increased. It is difficult to explain the observed change in current by anything other than the change of tire resistance with time.

The experiment that indicated the existence of two paths to ground, as illustrated in Figure 1, was the following: A conducting mixture of lampblack and Karo syrup was applied to the side walls of the tires. The conducting film extended from the metal wheel hub to within an inch of the outer edge of the side walls of the tires. The tread was not coated, so that the charge-producing mechanism was not affected, and the same total currents as previously produced would be generated at the tread of the tire. When the wheels were now set in motion, much higher currents to the car than previously encountered, were measured.

On the assumption that the total current generated at the tread of the tire is unchanged at a given speed, these results indicate that the total current must split into two paths, as shown in Figure 1, and that the ammeter reads only a portion of the total current generated.

Currents as high as eight microamperes at 40 miles per hour were obtained with a relatively thick coating of the conducting mixture. The resistance of the side walls was so decreased that two electrodes spaced three inches apart, indicated a resistance of approximately 100,000 ohms.

Furthermore, it is impossible to determine directly the total current generated at any particular speed. This can be seen if the circuit elements of Figure 1 are considered to constitute a Π network. From four-terminal network theory, it is known that three independent measurements must be made to determine the characteristics of the circuit. This means that two measurements must be made on one side of the network and one on the other. This is physically impossible, because no meters can be connected to that side of the circuit where the constant current I is being injected into the system.

However, if external resistances are inserted in series with the ammeter, and the current change measured, certain interesting results may be obtained. It is readily determined that

$$\frac{1}{I_w}=\frac{1}{I}+\frac{R_w}{IR_g}+\frac{R_e}{IR_g},$$ where R_e is the unlabeled external resistance in Figure 1.

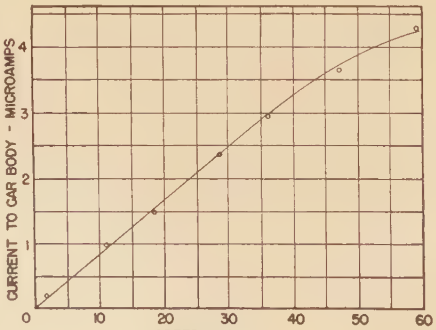

Figure 2. Typical curve of current measured from body of car to ground, as speed of rear wheels is changed

Thus, the reciprocal of the measured current for a given total current I, that is, for a constant speed, should vary linearly with the external resistance. The slope of the line is

$$\text{slope}=1/IR_g$$

and the intercept is given by

$$\text{intercept}=\frac{1}{I}\left(\frac{R_g+R_w}{R_g}\right)$$

and therefore

$$\frac{\text{intercept}}{\text{slope}}=R_g+R_w$$

Figure 4 shows the results of readings taken at a wheel speed corresponding to 40 miles per hour. Extremely high resistances had to be added in the ammeter circuit to obtain these data. It is seen that a straight line, predicted by analyzing the equivalent circuit, is actually obtained. This curve gives a value of $R_w + R_g = 1{,}325$ megohms. Further data, taken with the side walls made conducting, also yield a straight line; in this case, $R_w + R_g = 34.6$ megohms.

Without a knowledge of the total current I, it is not possible to determine the exact value of R_g and R_w. However, an idea of the order of magnitude can be determined in the following manner: as mentioned, with a relatively thick conducting layer, currents as high as eight microamperes at 40 miles per hour have been measured. We may assume that this is approximately the total current generated at this speed, since without any conducting layer, and with no external resistance, the measured current is 1.74 microamperes.

The slope of the curve of Figure 4 is $1/IR_g = 0.43 \times 10^{-3}$. Substituting the value of $I = 8 \times 10^{-6}$, we get $R_g = 290$ megohms. Since $R_w + R_g = 1{,}325$ megohms, then $R_w = 1{,}035$ megohms.

These results give the resistance of the two rear wheels in parallel. Consequently, the equivalent resistances for one tire would be $R_g = 580$ megohms, and $R_w = 2{,}070$ megohms.

This determination of R_w is in the range of the resistance between the hub of a wheel and a metal plate on which a tire is resting. The authors have made such measurements,[4] as have Cadwell, Handel, and Benson.[2]

If we assume that the total current generated by the tires is proportional to speed, we obtain, on the assumptions previously stated, a "proportionality constant" of approximately 0.1 microampere per mile per hour for each tire.

It will be noted that the value shown on

Figure 4 for the current measured at 40 miles per hour with $R_e = 0$, is 1.7 microamperes, or approximately one half of the current for this speed shown on Figure 2. Such changes were encountered over a period of time, and were probably caused by changes in the surface conditions of the tires.

Voltages Encountered

The slope of Figure 4 is given by $1/IR_g$. Accordingly, the reciprocal of the slope is the voltage to which the vehicle would be raised if all the current were dissipated to ground through the shunt resistance. This situation is approximated when extremely high external resistances, R_e, are used. For the data taken with the normal tire, this value is

$$V=(1/0.43)\times 10^3=2{,}320 \text{ volts}$$

During these experiments voltages of this size were measured, and Beach[1] likewise encountered voltages in this range, although somewhat higher. If an electrostatic voltmeter is applied to the car while on the dynamometer, instead of an ammeter, and one side of the voltmeter is grounded, then R_e will be the resistance of the front tire and the surface leakage over the floor to ground.

We made tests in which a layer of tinfoil was placed under the front tires, the tinfoil being insulated from the floor by rubber mats. The tinfoil was then grounded through the microammeter. As soon as the rear wheels started to turn, the microammeter indicated a current of about half the size previously encountered and a voltmeter connected between the car body and ground read several thousand volts. This, as stated above, is equivalent to inserting a very high resistance R_e in the ammeter circuit of Figure 1.

Those who previously made measurements of voltage, in attempting to analyze the problem of the tire static, were performing this experiment, without realizing that the voltages measured could be correlated directly with an *IR* drop through the front tires.

Table I. Data Obtained on Road

Speed (Miles Per Hour)	Current Measured (Micro-amperes)	R_e Meg-ohms	Remarks
10	3.0	0	All tests were made on an asphalt pavement
20	4.8	0	
10	2.5	100	
10	4.5	0	Conducting carbon layer on all four tires
10	1.5	0	Both right tires on grounded railroad track
10	2.0	0	Conducting carbon layer on right two tires, on railroad track
10	1.5	60	Conditions as above

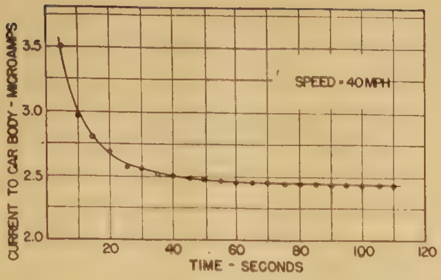

Figure 3. Variation of current to car body with time, wheel speed being held constant at 40 miles per hour

Road Tests

Data were also taken with vehicles actually in motion. However, it was rather difficult to get good readings. First of all, other cars passing nearby would induce temporary currents of the order of magnitude of those being measured. Mechanical meters, sensitive enough to read the current generated, were not rugged enough to stand the inevitable shocks received in a moving vehicle. Electronic meters were too susceptible to outside influence, such as power lines, and so forth. Despite this, the data obtained checked well with what might be anticipated from the dynamometer tests.

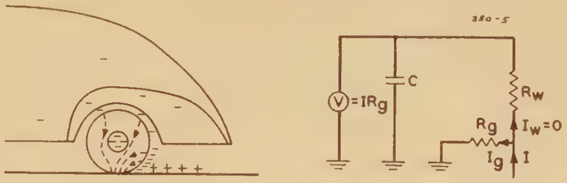

Figure 5. Equivalent circuit of a car moving on a highway

In the road tests, a chain about ten feet long was dragged in a railroad track, serving as a constant ground. The chain was insulated from the car by means of a wax-impregnated rope which also served as the strain absorber. The wire leading to the ammeter inside the car was insulated from the metal of the car by vacuum-held rubber cups. Accordingly, measurements could be made corresponding to I_w of the dynamometer experiments.

The most reliable data for the operating conditions detailed are listed in Table I. It is seen from this table that the order of magnitude of currents generated is that obtained on the dynamometer. Closer correlation is scarcely to be expected. The materials producing the charges were different: that is, on the dynamometer the rubber contacted metal, while in these tests the rubber contacted asphalt paving.

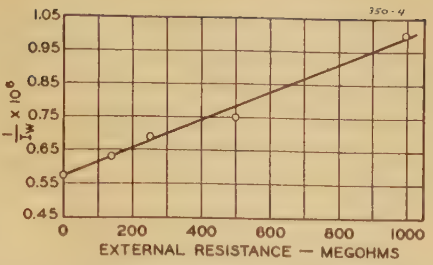

Figure 4. Reciprocal of current measured versus external resistance in ammeter circuit

Speed—40 miles per hour

We may see further, from the data taken with the right wheels in the grounded track, that reducing the value of the leakage resistance to ground will reduce considerably the amount of current going to the car. On the dynamometer stand, the path R_g, as mentioned previously, was the rubber between the charges and the grounded dynamometer, while on the road, the pavement resistance is added. Therefore, driving on the tracks should reduce the current to the car, since the leakage path to ground is now one of lower resistance. With the wheels on the track, additional resistance in the grounded part of the circuit has a much greater effect than if the wheels are not in the track, as may be seen from entries 3 and 7 in Table I. Figure 1 shows why this should be so. With the wheels in the track, R_g is relatively small; hence putting in additional resistance R_e will force more of the generated current through the leakage path to ground, thus decreasing the ammeter reading.

It is believed that the potential of a car in motion is determined primarily by the current produced by tires leaving the road. Other effects such as wind resistance and so forth, are believed to play only a minor role.

Conclusion

Figure 5 shows the equivalent circuit for a car moving on a road with uniform speed. In this circuit the capacitor C represents the capacitance of the car to ground. Obviously, once enough current has flowed through R_w to charge C up to a voltage equal to IR_g, then all the current will flow back to ground, and no more will flow to the body of the car. Thus, the car does not charge up indefinitely; the voltage to which it will rise is given by IR_g.

In order to decrease the potential acquired by a vehicle in motion, two methods may be used:

1. To reduce the amount of total charge produced. A method of doing this has already been proposed.[2]

2. To use tires with conducting tread and side walls. This would reduce the resistance R_g and, consequently, decrease the vehicle's potential.

Experience has shown that very seldom is a car sufficiently charged to give a static spark when driven into a service station. Even if we assume that a vehicle is brought to rest with a relatively high charge, this charge quickly leaks to ground. We have measured the resistance of the tires of a number of cars when at rest and found this resistance to be approximately 3×10^9 ohms for four tires in parallel.[3,4] The capacitance of a car is approximately 750 micromicrofarads. If we assume that a car is brought to rest with a potential of 3,000 volts, then we can make a rough calculation of the time required for the vehicle to discharge to 300 volts, which is the minimum sparking potential.

From the usual capacitor-resistance discharge equation

$$V = V_o e^{-\frac{t}{RC}}$$

Substituting $V = 300$, $V_o = 3{,}000$ $R = 3 \times 10^9$, $C = 750 \times 10^{-12}$ we find $t = 5.2$ seconds.

Furthermore, we have found in the laboratory that a capacitor of 750 micromicrofarads will produce a spark capable of igniting a combustible mixture of gasoline vapor and air, only if charged to a potential of several thousand volts. A spark may occur at a lower voltage, but it lacks the characteristics necessary to ignite a combustible mixture of gasoline vapor and air.

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Pilot-Wire Circuits for Protective Relaying—Experience and Practice

AIEE COMMITTEE ON PROTECTIVE DEVICES

RELAY SUBCOMMITTEE

BECAUSE of the increasing use of pilot-wire relays, particularly for longer lines, the relay subcommittee initiated an investigation of the operating experience with the pilot channel itself. From this investigation it was hoped to determine the requirements for a reliable pilot channel. Unfortunately, the information obtained to date is not conclusive, and under war conditions the completion of this investigation appears to be delayed unavoidably. On the other hand, plant expansion necessary to the war effort frequently has called for quick decisions on relay schemes, and pilot wires have been used in many cases. Because the pilot-wire channel is the least known item in the pilot-wire relay scheme, the committee has decided to publish the information now available to indicate the trend, so that prospective users of pilot-wire relaying may have the benefit of this preliminary work. Investigation will be continued as actively as conditions permit and another report issued as soon as sufficient data are available.

Table A

Protection at Terminals	Privately Owned Circuits	Leased Circuits
Carbon blocks	6 circuits of 2 companies	41 circuits of 9 companies
Air gaps	10 circuits of 2 companies	1 circuit of 1 company
Thyrite	7 circuits of 3 companies	1 circuit of 1 company
Protective tubes	17 circuits of 2 companies	None
Vacuum gaps	4 circuits of 2 companies	None
Fuses	16 circuits of 6 companies	19 circuits of 4 companies
Heat coils	2 circuits of 1 company	6 circuits of 2 companies
None	320 circuits of 10 companies	10 circuits of 2 companies
Not specified	1 circuit of 1 company	1 circuit of 1 company
Protection at Intermediate Point		
Carbon blocks	2 circuits of 1 company	48 circuits of 10 companies
Air gaps	7 circuits of 1 company	None
Fuses	6 circuits of 2 companies	15 circuits of 3 companies
Heat coils	2 circuits of 1 company	24 circuits of 7 companies
None	96 circuits of 6 companies	None
Not specified	273 circuits of 14 companies	1 circuit of 1 company

It should be remembered that the recent increased use of pilot-wire relay schemes has been occasioned by the development of new relays to give both phase and ground-fault protection over only two pilot wires. Because of the short history of these new developments, this report necessarily gives a rather incomplete picture of their performance, since it is influenced disproportionately by the much longer period of use of the older schemes. Further, it is probable that, in some of the cases where phase or ground-fault protection only is reported, the circuits would have been protected for both, had the newer schemes been available at the time. Also, the use of the newer schemes probably will increase the average length of pilot-wire circuits, because of their ability to operate over longer distances.

This report is based on the replies of 32 companies covering 436 circuits used for protective relaying. The information thus obtained covers 1,351 route miles of pilot-wire circuits and a period of 5,570 circuit years.

The following information, some of which is elaborated on later, would appear to be particularly significant.

1. Eighty-seven per cent of the circuits are privately owned, nearly all of which are in lead-sheathed cable. The average length is three miles. The longest circuit is 26 miles.

2. Two thirds of the circuits are installed on the same right of way or in the same duct

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Paper 43-27, recommended by the AIEE committee on protective devices for presentation at the AIEE national technical meeting, New York, N. Y., January 25–29, 1943. Manuscript submitted November 12, 1942; made available for printing December 29, 1942.

Personnel of the working group on pilot wires of the relay subcommittee: W. K. Sonnemann, sponsor; J. H. Neher, former sponsor (now with the United States Navy); J. C. Bowman, W. R. Brownlee, R. E. Cordray, R. F. Davis, L. L. Draper, E. E. George, S. Goldsmith, E. L. Harder, H. F. Lindemuth, W. E. Marter, P. N. Sandstrom, A. W. Walton, E. M. Wood.

Table I. Circuit Details and Performance of Leased Pilot Circuits

	Company Designation														
	4	5	9	13	15	16	19	21	22	23	24	26	28	29	32
Number of pilot circuits	4	7	11	4	1	1	1	1	2	1	8	10	1	4	2
Average length (miles)	6.4	2.4	6.3	4.6	1.5	7.4	8.0	12.1	2.4	4.6	10.6	15.1	6.0	5.0	*NS*
Maximum length (miles)	10.5	3.5	13.0	7.6	1.5	7.4	8.0	12.1	3.5	4.6	17.8	26.9	6.0	6.0	*NS*
Pilot-circuit performance															
Mile-years of operation	68	15	103	17	0	7.4	0	0	28	0	527	121	12	8	1
Total number of outages	15	0	79	23		1			12		59	9	2	2	0
Total hours interruption	15	0	140	142		4			55		167	28	500	2	
Caused by: Short circuits			5	2					*NS*		*NS*	5	1	1	
Caused by: Grounds	4					1			*NS*		*NS*		1		
Caused by: Open circuits	10		2	4					*NS*		*NS*	3			
Caused by: Circuit changes		[c]	51	16					*NS*		*NS*				
Caused by: Other causes	1		[d]21	[f]1					*NS*		*NS*	[f]1		[f]1	
Protective features[a]															
At terminals	*F*	*C*	*C*	*C* and *F*	*T*	*C*	*C*		*C* and *H*	*C* and *F*	None	*C* and *F*	*AG*	*C* and *H*	None
Voltage level		350	300	300		350	1,000		350	350			4,000	300	
At intermediate exchanges	*C* and *H*	*C*	*C*	*H*	*C* and *H*	*C* and *H*	*C*		*NS*	*C* and *F*	*C* and *H*	*C* and *F*	*AG*	*C*, *F*, and *H*	*H*
Voltage level	350	350	300			350	1,000			350	350		4,000		
Gradient protection[b]	None	4-*I* 3-*C*	11-*N* 4-*C*	4-*C* 2-*D*	*N*	*C*		*N*	*I*	*N*, *C*, and *I*	8-*I* 2-*C*	3-*N* 9-*C* 1-*I*	*C*	*I*	*C* and *I*
Insulation level (volts)		1,500 10,000	5,000	1,000	15,000				600		1,500 10,000		High		
Automatic supervision	Yes	No	Yes	2	Yes	No	Yes	Yes	Yes	Yes	Yes	Yes	No	No	Yes
Special circuit marking	Yes	Yes	Yes	Yes	Yes	Yes	Yes	?	Yes	Yes	Yes	Yes	Yes	Yes	Yes
Relay system															
Directional comparison	3	4	7					1			5		1	4	
Transferred trip					1				2		1				
Blocking	1											1			
2-wire a-c		3		4		1	1			1	2	9			2
3-wire a-c			4												
Phase faults only	2														
Ground faults only					1						2				
Phase and ground faults	2	7	11	4		1	1	1	2	1	6	10	1	4	2

(*a*). Symbols for protective features
AG—Air gaps
C—Carbon blocks
F—Fuses
H—Heat coils
PT—Protector tubes
T—Thyrite
VG—Vacuum gaps

(*b*). Symbols for potential gradient protection
C—Coupling transformers
D—Drainage equipment
I—Insulated relays
N—Neutralizing transformers

(*c*). Some planned interruptions.

(*d*). Twenty-one outages due to testing.

(*f*). Due to terminal protective equipment.

Table II. Circuit Details and Performance

	Company Designation											
	1	2	3	6	6	7	8	6	10	11	12	12
Number of pilot circuits	2	5	2	42	15	218	9	6	2	10	18	4
Average length (miles)	2.0	3.0	0.3	5.2	2.3	1.5	1.7	0.8	3.7	1.8	2.5	1.0
Maximum length (miles)	2.0	3.0	0.3	15.6	3.5	15.0	2.0	0.8	4.0	2.4	8.8	1.9
Pilot-Circuit Performance												
Mile-years of operation	0	208	1.0	529	147	6,000	57	21	7.4	292	178	31
Total number of outages		1	0	49	0	2,910	*NS*	4	0	0	18	6
Total hours interruption		5		1,418		11,640	*NS*	24			104	48
Cause: Short circuits				30							1	4
Cause: Grounds		1					4					
Cause: Open circuits												
Cause: Circuit changes				19				4		[f]	16	2
Cause: Other						[d]					1	
Cable Details												
Conductor size (American wire gauge)	16	12	14	19	12	12	12	12	19	8	19	10
Insulation[a]	3/64 inch *R*	3/64 inch *R*	*R*	*P*	*R*	3/64 inch *R*	3/64 inch *R*	3/64 inch *R*	*P*	3/64 inch *R*	*P* and *R*	Open
Protective features at terminals[b]	*PT*	None	None	None	None	None	*PT*	*PT*	*C-H-F*[e]	None	4-*F* 2-*T*	None
Voltage level (60 cycles)							450 volts	450 volts	354 volts		[g]	
Potential gradient protection[c]	*C*	None	None	17-*C* 25-*I*	11-*C* 4-*I*	*I*	None	None	None	None	17-*C* 1-*I*	4-*C*
Insulation level (volts)		600		600	600	1,500						
Automatic supervision	No	No	No	14	1	No	Yes	Yes	Yes	1	6	No
Special circuit marking	No	*NS*	Yes	Yes	Yes	*NS*	*NS*	*NS*	Yes	*NS*	1	No
Relay system												
Directional comparison		3		19			15			1		
Transferred trip		5		10								
Blocking									2		1	
2-wire a-c	2			18		203					20	
3-wire a-c			2	10								
4-wire a-c						15				9	1	
Phase faults only				30						9		
Ground faults only		5				15						
Phase and ground faults	2		2	27		203	15		2	1	22	

(*a*). *R*—Rubber, *P*—Paper } thickness as specified

(*b*). Symbols for protective features
AG—Air gaps
C—Carbon blocks
F—Fuses
H—Heat coils
PT—Protector tubes
T—Thyrite
VG—Vacuum gaps

(*c*). Symbols for potential gradient protection
C—Coupling transformers
D—Drainage equipment
I—Insulated relays

(*d*). Over 95 per cent of outages are "planned interruptions."

(*e*). *C*, *H*, and *F* at intermediate stations also.

(*f*). Some planned outages for circuit changes.

system as the power cables. The rest are leased circuits or privately owned telephone cables which generally are located at a distance from the power circuits.

3. All but two of the circuits are metallic. In these two cases ground return circuits are employed. In six cases the circuits are used for other purposes besides relay protection.

4. Although automatic supervision is used only on 18 per cent of the total installations and by 50 per cent of the companies reporting, nearly every company reporting indicated that it would use automatic supervision on future installations.

5. The service continuity reported based on 5,570 circuit years indicates an average out-of-service time of less than 0.0335 per cent. It appears that the interruptions on the leased circuits are more frequent but of shorter duration than on the privately owned circuits. An analysis indicates 1.32 interruptions per circuit year, with an average of 3.54 hours outage per circuit year for the leased circuits, while the privately owned circuits have an outage once in 1.74 years, with an average outage time of 2.92 hour per circuit year. The out-of-service time includes "planned interruptions," or circuit rearrangements.

6. Carbon block and fuse or heat-coil protection is applied to most of the leased circuits, both at the terminals and en route, and special tagging is employed in the various exchanges. Only 20 per cent of the privately owned circuits are provided with terminal protection. Seventeen circuits are reported as being equipped with neutralizing transformers.

Circuit Details

Ownership. Fifteen companies reported 58 leased circuits, while 20 companies reported 378 privately owned circuits. Only three companies reported having circuits of both types.

Construction. As previously indicated a large majority (367) of the privately owned circuits and apparently all of the leased circuits are in cable. The size of the conductor in the privately owned cables ranges from number 6, where the cable is used exclusively for pilot-wire relaying, to number 22, where the cable carries telephone and supervisory circuits as well as pilot-wire circuits. The larger wire sizes are generally rubber covered. In most cases lead sheathing is employed.

Electrical Characteristics. Although the average length of the circuit reported is three miles, in several cases circuits of 15 to 26 miles are in operation. The resistance ranges from a few ohms to as high as 2,675 ohms.

In certain cases involving leased circuits, loading coils are employed. It appears that these loading coils are an inherent part of the circuit facilities leased and are not introduced specifically for the pilot-wire system. None of the

of Privately Owned Pilot Circuits

Company Designation												
14	15	17	18	19	20	25	27	27	28	28	31	30
2.0 1.0 1.0	2.0 1.0 1.5	13 2.5 3.5	3.0 4.3 5.0	3.0 1.5 2.2	1.0 3.2 3.2	3.0 4.3 6.0	1 0 0.5 0.5	2.0 2.5 3.5	6.0 4.3 9.0	3.0 10.3 16.0	1.0 17.9 17.0	5.0 5.6 12.5
2	20	0	16	8.0	0	16.5	1.0	13.5	150	151	6.0	180
0	0		0	1.0		0	0	0	[l]8	[m]12	0	102
				74 0					1,048	449		1,034
												20
												18
												51
				[j]1								13
19	6, 10	22	19	19	12	16	8	6	8, 16	Cable and open	Open wire	8
600 volts[h]	*P*	[i]*P*	*P* and *R*	[k]*P*	4/64 inch *R*			Open	600 volts 1,500 volts			Open wire
None	*T*	None	*F*	None		*VG*	*T*	*T*	3-*AG*	3-*AG* 1-*F*	*VG* *F*	4-*AG*, 4-*C* 5-*F*
									4,000 volts	4,000 volts		
C	1-*C*	*C*	None	*I*	*C*	*C*	None	1-*I*	None	*C*	*D*	4-*I* 1-*C*
		1,400				1,800			High	High		45
No	No	*NS*	Yes	No		Yes	No	Yes	No	No	No	No
NS	No	No	*NS*	Yes		Yes	No	*NS*	3	Yes	No	4
	2		3				1		6		1	
							1					
												4
2		13		2	1	3						1
				1			1		3			
	2			1			3					
2		13	3	2	1	3			9		1	5

(*g*). Four circuits with fuses at intermediate stations.

(*h*). Special tests showed maximum induced voltage to ground of less than 565 volts.

(*i*). One mile rubber insulated. Some cable 1,500 volts to ground, and some 3,500 volts to ground.

(*j*). One outage due to flood conditions.

(*k*). 4.13 miles number 19 double paper insulation. 0.35 mile number 4 rubber insulation.

(*l*). All cable failures. Present condition believed to be capable of better performance.

(*m*). Three outages due to some cable troubles as in *l*. Remainder mostly sleet on open wire sections

(*n*). Ground return circuit.

NS. Not specified in reply to questionnaire.

relay schemes were reported as actually requiring the use of loading coils for their proper operation.

Terminal and Intermediate Protection. It appears that, in most cases, standard telephone protective equipment such as carbon blocks, fuses, and heat coils is applied to leased circuits at the various telephone exchanges through which they pass and also at the line terminals in the power stations. Some of the privately owned circuits are treated similarly at the line terminals. Air gaps, vacuum gaps, and Thyrite are employed. The reported terminal and intermediate protection is shown in Table A.

At times of ground faults on power systems, a considerable difference in potential may exist between the power-station ground as represented by the primary relay equipment and true ground as represented by the pilot-wire circuits. Certain questions were asked with the intent of determining if this was a serious problem and how it was cared for. Unfortunately the intent of the question was in many cases misunderstood, and the replies received lead to the belief that in many cases this problem has not been given due consideration. In certain cases, however, neutralizing transformers have been installed and in other cases highly insulated relay assemblies or insulating transformers have been indicated. Although insulating transformers constitute a component part of many of the pilot-wire protective systems employed, nevertheless, in some cases the insulation value of this equipment has been nullified with respect to the supervisory equipment installed on the line side and connected to the power-station ground.

The reported potential gradient protection and grounding practice is shown in Table B.

Circuit Performance

Service Continuity. Table C indicates the record of pilot-wire outages of 26 companies.

Causes of Pilot-Wire Outages. The cause of pilot-wire outages reported by nine companies covering 409 circuit-years is shown in Table D.

Trouble Due to Induction. Trouble due to induction or surges was indicated in 20 per cent of the circuits reported. Apparently the majority of these troubles have been corrected.

Protective Relay Systems

As a matter of incidental interest, information was obtained in regard to the relay systems utilized.

Types. The following general types of pilot-wire protective systems were

Table B

Neutralizing transformers	17 circuits of 5 companies
Line-coupling transformers	102 circuits of 19 companies
Insulated relay equipment	278 circuits of 14 companies
No protection	50 circuits of 9 companies
Ungrounded	298 circuits of 9 companies
	14 circuits of 2 companies with neutralizing transformers
	21 circuits of 5 companies with coupling transformers
	7 circuits of 2 companies with both neutralizing and coupling transformers
	256 circuits of 6 companies with neither neutralizing nor coupling transformers
Grounded to pilot cable sheath	3 circuits of 2 companies
Grounded through station battery equipment	4 circuits of 1 company
Grounded to station ground	65 circuits of 13 companies
	30 circuits of 6 companies with coupling transformers
	11 circuits of 2 companies grounded at one end only
Grounded to a remote ground	17 circuits of 5 companies
	6 circuits of 3 companies with coupling transformers
	6 circuits of 1 company grounded at one end only

Table C

Ownership	Company	Circuit Years	No. of Outages	Total Hours	Outage Per Circuit Per Year	Average Time Per Outage (Hours)	Outage in Percentage of Service
Private	2	68	1	5	0.0147	5.00	0.00084
	3	8	0	0	0.0		0.0
	5	4	0	0	0.0		0.0
	6	166	49	1,418	0.295	28.9	0.0975
	7	4,701	2,910	11,640	0.619	4.0	0.0282
	8	56	0	0	0.0		0.0
	10	2	0	0	0.0		0.0
	11	162	0	0	0.0		0.0
	12	119	24	152	0.202	6.35	0.015
	14	2	0	0	0.0		0.0
	15	13	0	0	0.0		0.0
	18	4	0	0	0.0		0.0
	19	5	1	74	0.2	74.0	0.169
	25	5	0	0	0.0		0.0
	27	6	0	0	0.0		0.0
	28	68	20	1,497	0.294	74.9	0.251
	30	28	102	1,034	3.64	10.12	0.421
Total	17	5,417	3,107	15,820	0.574 average	5.08 average	0.0333 average
Leased	4	12	15	15	1.25	1.00	0.014
	9	50	81	130	1.62	1.605	0.0297
	13	4	23	142	5.75	6.18	0.405
	16	1	1	4	1.00	4.00	0.046
	22	22	12	55	0.545	4.59	0.0285
	24	52	59	167	1.135	2.83	0.037
	26	7	9	28	1.285	3.11	0.046
	29	4	2	2	0.50	1.00	0.006
	32	1	0	0	0.0		0.0
Total	9	153	202	543	1.32 average	2.68 average	0.0355 average
Grand total	26	5,570	3,309	16,363	0.594 average	4.94 average	0.0335 average

* This item was reported as over 95 per cent "planned interruptions." Companies with new installations with less than one year of operating experience are not included in this table.

reported covering 436 circuits of 32 companies.

Directional comparison	76 circuits of 16 companies
Transferred trip	20 circuits of 6 companies
Blocking	9 circuits of 5 companies
Two-wire—a-c	288 circuits of 18 companies
Three-wire—a-c	21 circuits of 6 companies
Four-wire—a-c	25 circuits of 3 companies

These circuits furnish protection as follows:

Phase faults only	48 circuits of 7 companies
Ground faults only	22 circuits of 3 companies
Phase and ground faults	366 circuits of 31 companies

Backup protection is employed on the majority of circuits.

Performance. The performance record based on 406 circuits of 25 companies having 5,500 circuit years experience indicates a total number of 381 faults on protected lines with 368 (96.5 per cent) correct relay operations. In addition, a total number of 89 incorrect trippings was reported. Information available, however, indicates that a considerable number of these occurred during the first few months of service.

Table D

Circuit rearrangement	118
Short circuits	87 (6 of which were grounds)
Defective terminal equipment	46
Open circuits	37

Testing Practice. A total of 30 per cent of the circuits are tested yearly, 66 per cent semiannually, 2 per cent every three months and 2 per cent every month. It appears that the presence or absence of automatic supervising equipment has no influence on the frequency of test. In addition certain circuits not equipped with automatic supervision are checked for continuity, either daily or on every shift.

Conclusions

In conclusion it may be stated that an analysis of the replies received indicates that pilot-wire circuits have performed in a satisfactory manner and that the use of this form of protection is increasing. Certain difficulties experienced initially have been overcome successfully in practically all cases. Only one company reported the discontinuance of this form of protection because of unsatisfactory experience.

The replies to the questionnaire as received from the various operating companies have been tabulated and are given in Tables I and II.

Cooling Power Transformers by Forced Circulation of Cooling Medium

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Synopsis: The paper discusses the various methods that have been used in cooling power transformers by forced circulation of the cooling medium, and points out the benefits

1. To the war effort in the saving of critical materials.

2. To the user in first cost, shipment, installation, station layout, and mobility.

The selection of proper control equipment is analyzed from the standpoint of protection and reliability of forced cooling.

The relation between hot-spot and average winding temperatures and the future for forced cooling after the war is discussed.

General

A FEW years ago a study made of the service experience with transformers indicated that a satisfactory level of reliability had been reached. It was decided then to direct the major efforts of developmental work towards greater economy of transformer application by making savings in material, space, and weight and at the same time maintaining the level of reliability demanded by industry. To accomplish this aim many novel ideas were incorporated into a new transformer design. A developmental single-phase transformer of 14,500 kva, 135,000 to 11,000 volts was built. Figure 1 shows the core and coils with cover and bushings attached. In this novel unit radical savings in weight and dimensions were made as compared with a conventional self-cooled transformer of the same rating having the same electrical characteristics. Data obtained on this transformer demonstrated that such savings can be realized.

Further saving would have resulted from the use of the high-permeability cold-rolled silicon steel used in wound-core transformers.

This transformer was metal-sealed and designed to be shipped completely assembled, filled with insulating liquid, and with bushings in place.

The windings were made concentric cylindrical type which are ideally adapted to forced cooling. A cross section of these windings is shown in Figure 2. Forced cooling was one of the most important features of this new design.

I. Benefits to the User of Forced-Cooled Transformers

1. Significant reduction in the first cost of the transformer due to great reduction in total material.

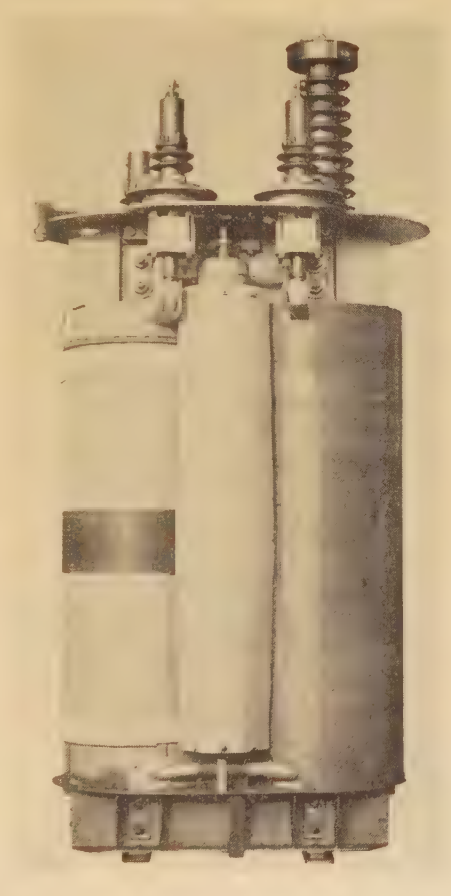

Figure 1. Internal view of a single-phase 14,500-kva 135-kv grounded-wye power transformer cooled by forced-air and forced-oil flow

2. The greatly reduced weight results in:

(*a*). Saving in transportation cost.

(*b*). Saving in installation expense, both in the foundation and handling.

(*c*). Almost doubling the maximum kilovolt-amperes that can be shipped completely assembled, where weight is the limiting feature.

3. A reduction of about 50 per cent in over-all volume of transformer means:

(*a*). A saving in floor space and head room of installation.

(*b*). Easier handling operations.

(*c*). Raising to about three times the maximum kilovolt-amperes of a transformer that can be shipped assembled, where size is the limiting feature.

4. Shipping the transformer completely assembled with all cooling equipment attached means:

(*a*). Economy of time and money at installation which may amount to several thousand dollars per transformer, for a very large unit.

(*b*). Greater security of operation because of elimination of field work.

5. Shipping transformers completely assembled permits metal sealing *of practically all oil joints*, such as bushings, cover, and so forth, and means:

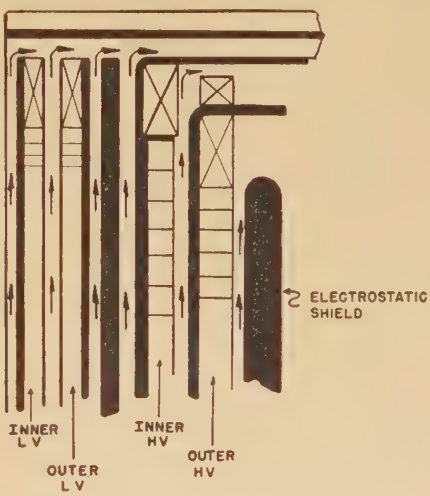

Figure 2. Cross section of top end of windings of transformer in Figure 1 showing directed oil flow

(*a*). Elimination of practically all gaskets.

(*b*). Permanence of tightness of joints, with greater strength.

(*c*). A reduction in the chances for oil contamination by moisture and oxygen from the air.

6. In transformers where the watts per square inch dissipated are low, or where the turn and coil insulation is relatively thin, there is a reduction in the difference between the average temperature of the winding and the hottest spot. This permits overloading of the windings without exceeding 65 degrees centigrade hot-spot rise, thereby gaining in economy of transformer design without a reduction in reliability.

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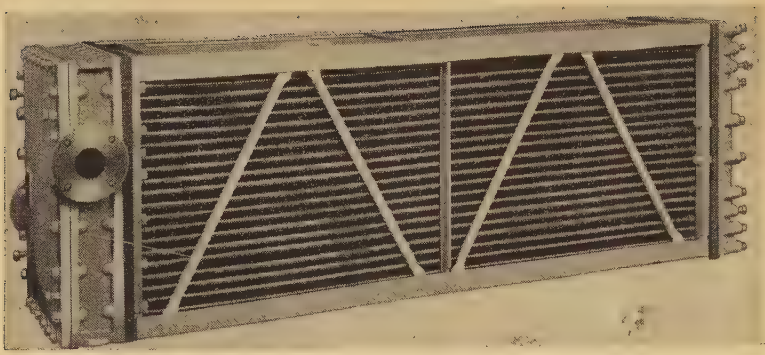

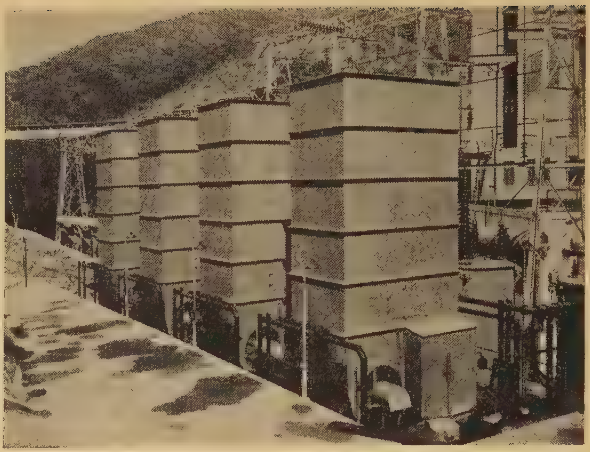

Figure 3 (above, and right). One of the first applications of oil air coolers with finned tubes used to cool transformers by forced circulation of oil and air

7. Remote installation of cooling equipment may be applied to either new or old transformers; this introduces new possibilities in station layout with the opportunity of utilizing the heat dissipated by the cooler.

II. Reliability

For some decades large transformers have been built with forced circulation of the oil, generally cooled by an oil-water cooler, with the oil circulated through both the transformer tank and the cooler by means of a pump. Several million kilovolt-amperes of transformers built in this way are in service today. Each unit is provided with its pump and cooler. Service experience has been entirely satisfactory, except for the inconvenience occasioned by the leakage of oil through the stuffing box of the pump and also the maintenance of the stuffing box. Instances of large installations of the oil-water cooled-type transformers built by the company with which the authors are associated are 28 units of 26,667-kva 161-kv single-phase transformers and 10 units of 27,500-kva 230-kv single-phase transformers.

An application of the oil-air cooler type made about a decade ago includes several 25,000-kva transformers which were cooled by forced circulation of oil through the transformers and through the fin-type coolers with a blast of air forced rapidly across the external surface of the fin tubes (Figure 3).

During the past several years, a large number of forced-cooled transformers have been built for use on electric locomotives as shown in Figure 4. This transformer design was the result of a joint undertaking by the Westinghouse Electric and Manufacturing Company and the General Electric Company.[4,5] In this transformer, the Askarel,* cooled by a rapid blast of air, was not only forced through the cooler, but also directed and forced through the coil ducts in the windings at a velocity much higher than normally created by the thermal head produced by the losses in a transformer winding. Also, it is many times the velocity used in the transformers previously mentioned, where the oil-water and the oil-air coolers were used. An excellent service record has been maintained in this locomotive application.

Service experience with small cooling fans for over a decade has been unusually successful, since over 15,000 of these fans have been installed on air-pressure cooled transformers, *Type OAP, totaling millions of kilovolt-amperes and to the best of our knowledge without a failure of one fan or its motor.*[6,7,8] Furthermore, much experience with fans and fan motors was secured with air-conditioning devices used for industrial purposes.

Based upon this extended field experience in the application of oil pumps, motors, and fans, we felt justified in presenting this type of station and portable transformer, which utilizes all of these accessories (Figures 5, 6, 7). The interest shown in this new development during the last few years confirms this viewpoint.

While in the present emergency the scarcity of materials gave strong impetus to the use of forced-cooled transformers,

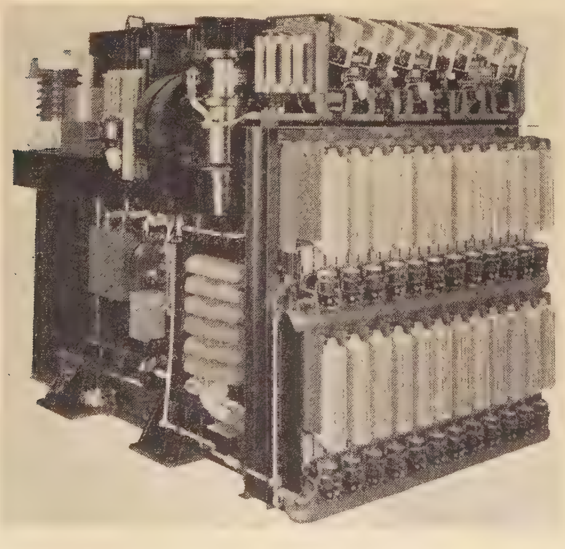

Figure 4. A 4,850-kva 12-kv electric-locomotive transformer cooled by forced-air and directed forced-oil flow

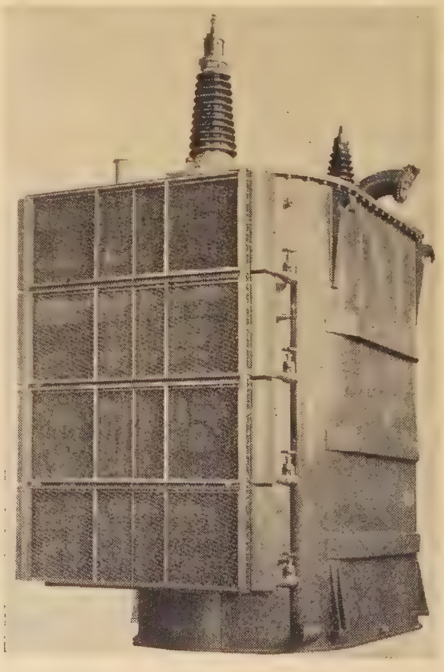

Figure 5. A 40,000-kva 135-kv power transformer cooled by forced-air and forced-oil flow

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* Noninflammable synthetic insulating and cooling medium.

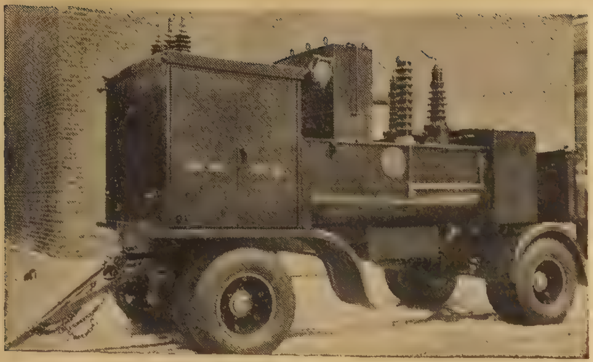


Figure 6. A 2,500-kva 33-kv mobile substation with transformer cooled by forced-air and directed forced-oil flow

the development and application of this type of transformer was undertaken before the emergency and was justified by normal conditions.

III. Nondirected- and Directed-Flow Forced-Cooled Transformer

There are two types of forced-cooled transformers: those with *nondirected* and with *directed* flow of oil through the windings.

Whenever the oil from the pump flows freely inside the tank, with the flow of oil through the windings produced by the thermal head or natural convection, the transformer is a *nondirected*-flow type (Figure 3).

On the other hand, if by the use of oil stops or baffles, the principal part of the oil from the pump is forced to flow through the windings of the transformer, it is a directed-flow type (Figures 1, 2, 4).

With nondirected flow, the advantage gained by forced cooling is that the oil is cooled to a predetermined value, which permits the use of a shorter tank and a reduction in size, weight, and cost of the cooling means (see item 4 of Table I). Some reduction in the amount of material used in the core and windings is possible.

With directed flow, additional savings are possible in the material used in the core and winding, resulting in a further reduction in the total weight of the transformer, as shown by items 5 and 6 of Table I.

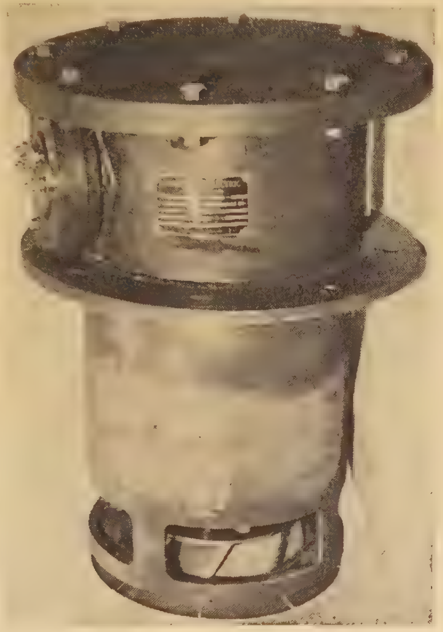
In both of these cases, the cooling equipment alone occupies about ten per cent of the volume and 25 per cent of the weight of radiators required by a self cooled transformer to dissipate the same loss with the same oil rise.

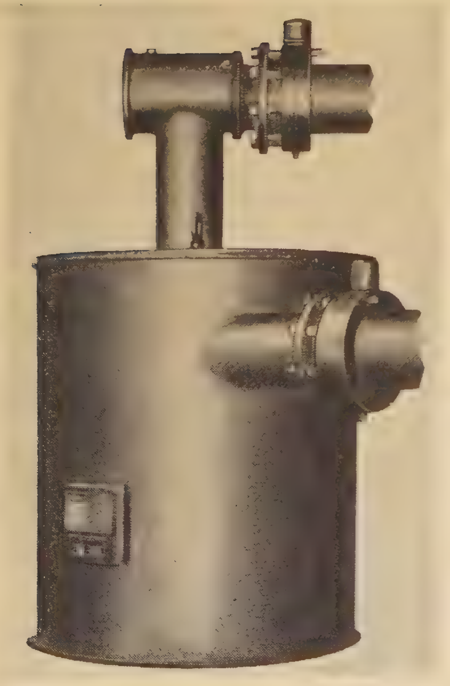
Nondirected flow may be applied to transformers already installed, either to increase the output, or to eliminate the use of water in water-cooled transformers.

For application of directed flow, a standard line of very economical cooling equipment has been developed and covers the entire range of power transformers. To obtain minimum cost, advantage was taken of mass production of these forced-oil forced-air coolers.

The fan motors are protected by overload relays built into the motor to prevent damage, in case of accidental stopping or blocking of the fan (Figure 8).

To eliminate the troubles with pump stuffing boxes, and to further increase reliability of the pump, a variety of oil-immersed motor pumps has been developed to serve transformers for the various applications (Figure 7). The motor is immersed and rotates in the transformer oil, which circulates continuously through the motor jacket, to cool the motor windings and to lubricate the bearings.





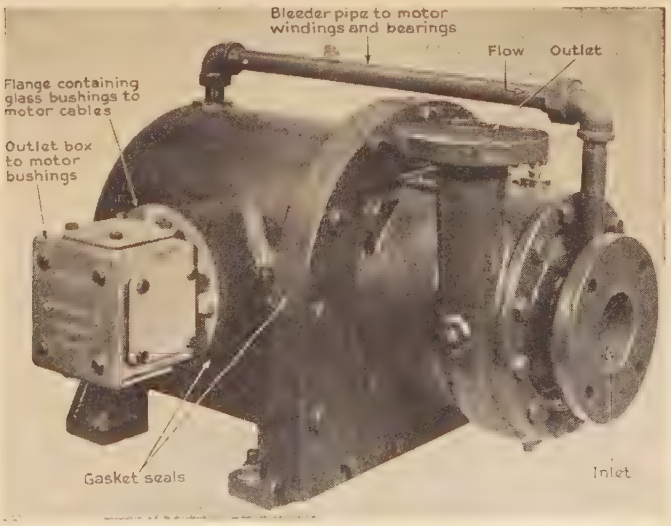

Bleeder pipe to motor windings and bearings
Flow
Outlet
Flange containing glass bushings to motor cables
Outlet box to motor bushings
Gasket seals
Inlet

Figure 7. External views of oil-immersed motor pumping units

IV. Cost of Forced Cooling

The cost to the user of coolers with forced flow is not limited to the cost of the cooling equipment alone but must also include the cost of the capitalized power input to operate the fans and pump motors. In addition, the cost of the cooling water must be considered whenever forced-oil forced-water coolers are used (Figure 9). Figure 10 shows the relation between the relative cost of the cooler and the power input to the cooling mediums—also the combined cost of the power and the cooler. Various pronounced economical balances exist between the cost of the cooling equipment and the cost of power input.

Wherever capitalization of power is high, further economy in operation may be obtained by sectionalizing the cooling equipment, controlling it by the temperature of the windings or the oil, and using only that portion of the cooling equipment necessary to maintain the desired temperature. It should be noted, however, that this practice causes a reduction in insulation life.

V. Hot-Spot Temperature of the Winding

The life of a transformer winding depends upon the temperature of its hottest spot and not on its average temperature. A margin of ten degrees centigrade between the average and the hot-spot temperature is specified by AIEE rules, because the measurement of the temperature of hottest spot during the commercial acceptance test is impractical.

The heat generated in a winding conductor, depending on the position of the conductor in the winding, passes consecutively through from 9 to 12 thermal regions before it reaches the ambient cooling medium, whether it be water or air. The temperature drop through each one of these regions is governed by a different law, but in spite of this our developmental and commercial tests indicate that the average temperature of a transformer winding cooled by thermal oil circulation can be computed within one to three degrees centigrade.

Table I

	Oil in			Air Outside Radiator	Water Outside Radiator	Total Weight
	Winding	Tank	Radiator			
1	Thermal flow	Thermal flow	Thermal flow	Thermal flow		100
2	Thermal flow	Thermal flow	Thermal flow	Forced		82
3	Thermal flow	Thermal flow	Thermal flow	Thermal flow	Spray	79
4	Thermal flow	Forced	Forced	Forced		73
5	Forced	Forced	Forced	Forced		55
6	Forced	Forced	Forced	Forced	Spray	50

Water-cooled and forced-oil forced-water transformers are not included.

The causes for differences in temperatures in various parts of the windings may be grouped in four classes; solid insulation, distribution of losses, velocity of oil flow, and oil temperature gradient.

Solid Insulation. From the standpoint of temperature, solid insulations between copper and adjacent oil can be subdivided as follows:

1. Insulating tape on coils.
2. Turn insulation.
3. Spacing strips and channels.
4. Insulating collars.
5. Layer insulation.

Distribution of Losses. Heat generated in the copper of the various parts of the winding may vary because of:

1. The variation in eddy loss caused by the difference in location of the coil or turn in the leakage flux field.
2. Differences in eddy loss due to variations in the size of conductor and its width and thickness.
3. Unequal current distribution between parallel conductors in the case of imperfect transpositions of these conductors.

Velocity of Oil Flow. Variations in velocity of oil flow may be caused by:

1. A change in cross section of an oil duct.
2. The distribution of oil between parallel oil ducts of unequal hydraulic resistance, caused by difference in length or cross section of the duct, and disposition of the spacers and insulating collars.
3. The interference of the core leg in interleaved winding construction.

Oil Temperature Gradient. The temperature of the oil in its course through the winding increases continuously as the oil absorbs heat from the coils.

VI. Comparison of Temperature Distribution in Self-Cooled and Directed Forced-Cooled Windings

Since one of the main advantages of directed forced cooling of transformers is the radical reduction in weight of the

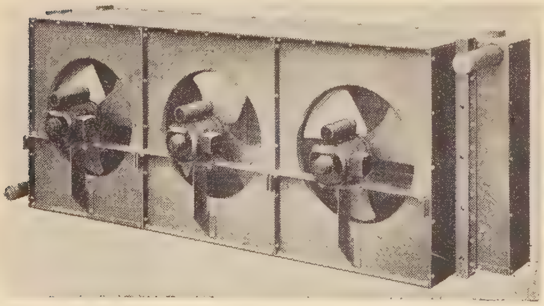


Figure 8 (left). Three-fan section of oil air cooler

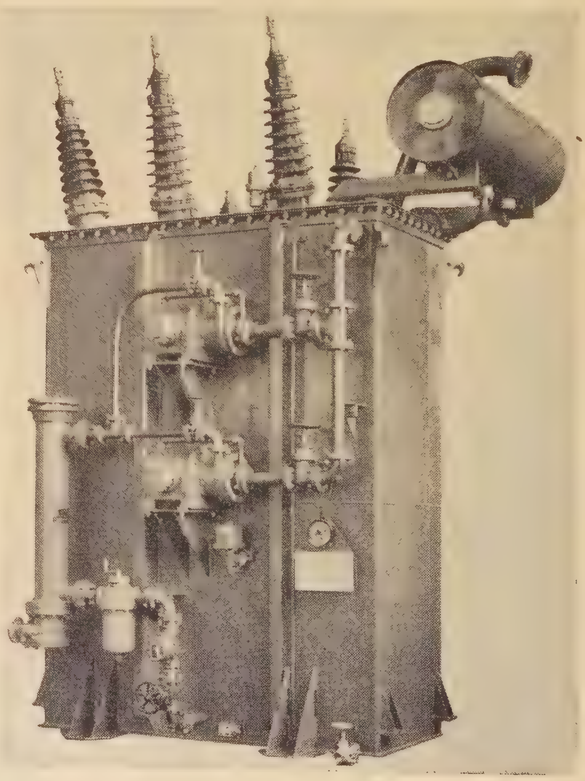


Figure 9 (right). A 7,250-kva 69-kv power transformer cooled by forced water with forced-oil flow windings, the copper loss per pound of winding is higher in forced-oil cooled than in self-cooled transformers. On the other hand, in forced cooling, where the heat transfer from the winding to the oil is more effective for a given temperature rise, relatively smaller winding surface needs to be exposed to the oil. For these two reasons, the watts per square inch of winding surface exposed to oil in a directed forced-oil cooled transformer is usually considerably greater than that used in a self-cooled transformer.

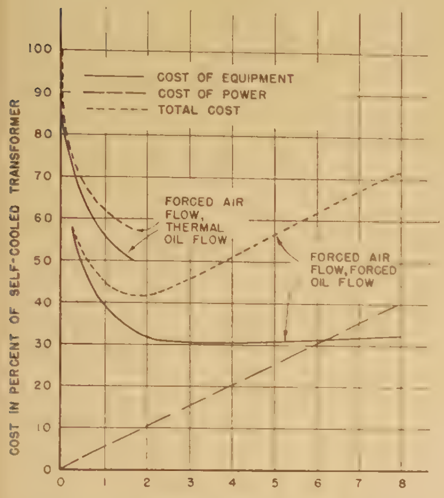


Figure 10. Relative cost of cooling transformer oil as a function of the power input to the cooling equipment

Furthermore, because of improvement in the space factor of the windings and the use of a smaller core section, the leakage flux density may be substantially higher than in self-cooled transformers.

Therefore, the temperature variations produced by nonuniform distribution of solid insulation and of losses are greater in directed-flow forced-cooled transformers than in self-cooled transformers.

In self-cooled transformers the motive-force causing heat-bearing oil to move away from hot surfaces increases with the amount of heat dissipated. Therefore, every part of the winding exposed to the oil and dissipating heat provides its own local motive force to carry away the heat. This tends to reduce the difference in temperatures produced either by restrictions to oil flow or by variation in heat dissipated per square inch of winding area.

On the other hand, in directed forced-cooled transformers, the oil circulation is produced by the oil pump, and at times its direction may be even contrary to the thermal flow of the oil. Therefore, the self-equalizing effect that plays such an important part in minimizing temperature differences in self-cooled transformers is essentially absent in forced-cooled transformers. For this reason it is very important that the complete structure be designed so that the forced oil flow distributes in a manner that produces predetermined temperature distribution.

The oil-temperature gradient, that is, the difference in temperature of oil in contact with the winding at the entrance and exit of the oil path, is proportional to the heat absorbed by the oil and inversely proportional to the velocity of oil flow.

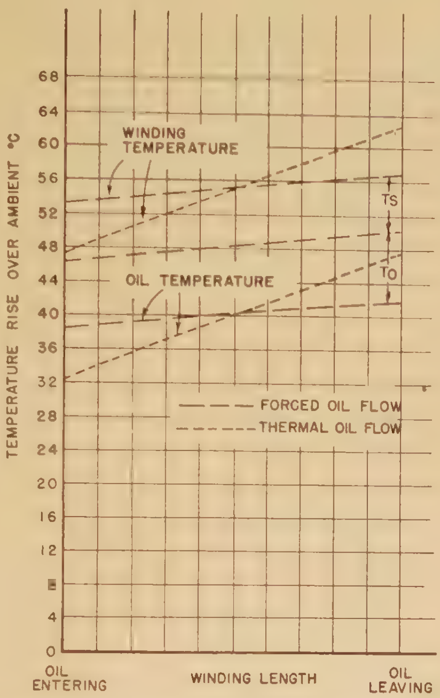


Figure 11. Comparison of oil temperature gradient and the resultant winding gradient in self-cooled and directed forced-cooled transformer

In both self-cooled and forced-air-cooled transformers, this oil gradient is of the order of 10 to 15 degrees centigrade. In directed forced-cooled transformers, the amount of oil forced through the winding is many times greater than would flow naturally because of winding heat, and therefore the gradient is proportionately smaller, being only a few degrees. This is one of the very important thermal characteristics of directed flow, forced-cooled transformers, that is responsible for the reduction in difference between the hot-spot and average winding temperature (Figure 11).

For example, with a gradient of 15 degrees centigrade for thermal oil flow, and three degrees centigrade for directed forced-oil flow (meaning that five times as much oil is forced as would flow naturally), and assuming the average oil temperature and all other factors influencing the temperature to be the same, part of the winding at the exit end of the oil path in the first case will be approximately six degrees centigrade higher than in the second case. It also follows that the difference between average and hot-spot winding temperatures, contributed by oil-temperature gradient alone, in the forced-flow case is only $1^1/_2$ degrees centigrade. However, it would be a fallacy to assume that this represents the entire difference between the hot-spot and the average temperature, since it would require neglecting all other contributing factors.

VII. Types of Oil Flow in Windings

Commonly, two types of coils are used in power transformer windings; pancake or disk coils, and cylindrical or barrel coils. Through these two types of windings, three directions of oil flow are possible: axial, radial, and transverse, as shown by Figure 12. In the axial and radial flows, the oil stream is parallel to the spacers. In the transverse flow, most of the spacers cross the oil stream and cause it to follow a complex path.

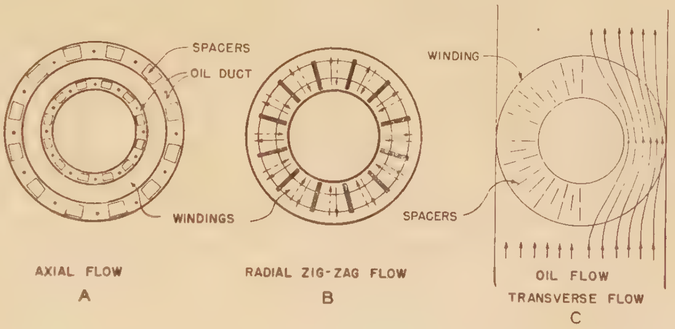


Figure 12. Three types of directed forced-oil flow through windings

In forced-cooled transformers, the temperature drop through the oil film at the coil surface depends upon the oil velocity in the duct, as shown by Figure 13. Therefore, when it is desired to maintain uniform temperature rise of copper over adjacent oil with thickness of solid insulation and uniform watts per square inch on the coil surface, it is necessary that the oil velocity be constant along the winding surface.

In concentric cylindrical coils the spacers are axial and the ducts are uniform in cross section; therefore, the oil flow is parallel to the spacers, and its velocity is constant (Figure 12A).

With radial flow, the oil velocity at the inner edge of the disk coil is higher than at the outer edge, in the ratio of the perimeters (Figure 12B). In power transformers, with the high- and low-voltage windings located concentrically, the radial width of the disk coil is a small fraction of its mean radius; therefore, the difference in the oil velocities is very small.

With transverse flow, the core leg or the insulating barriers inside the coil causes considerable variation in cross section of the oil duct. The ratio of the maximum cross section of the duct to the minimum is equal to $D/D-d$, where D is the outside diameter of the disk coil and d the inside diameter. It results in a ratio of oil velocities in the two parts of the duct of $V_2/V_1=D/D-d$, which may be 2:1, and sometimes even higher.

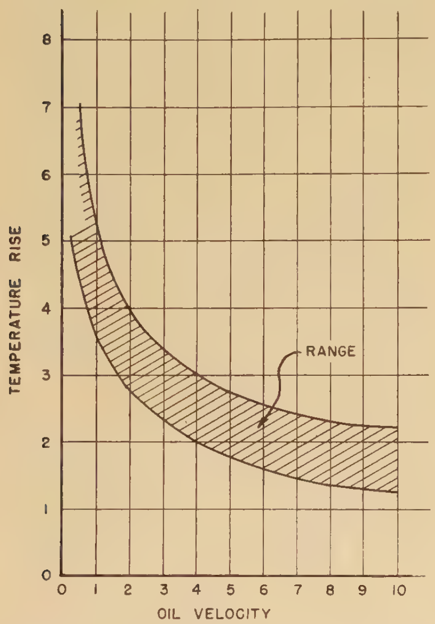


Figure 13. Relation between velocity and temperature rise of windings over oil for same watts per square inch on the coil surface

Further, the core or the insulating cylinder inside the coil "casts a shadow" on the downstream side, so that part of the coil surface may have an oil velocity, V_3, only one half of the entrance velocity, V_1. On account of this unequal distribution, the ratio between the maximum and the minimum velocity V_2/V_3 may be as high as 4:1. This unequal distribution of oil flow may cause a temperature drop across the oil film in the "shadow" to be a fraction of a degree to several degrees higher than in that part of the winding where the velocity of the oil flow is the maximum.

Uniform oil velocity is desirable where losses per square inch of winding surface and the thickness of the solid insulation between the copper and the oil are uniform. However, wherever these two factors vary, it may be desirable to modify the oil velocity to counterbalance the increase in temperature caused by either greater thickness of insulation or higher watts per square inch dissipated by the oil surface.

For example, at the ends of both high-voltage coils, Figure 2, extra turn insulation is used to withstand possible concentration of transient voltage stresses. In addition, the watts per square inch on the inner coil adjacent to the low-voltage winding may be higher than in the outer coil on account of increased eddy losses. This occurs wherever a coil is adjacent to the opposite winding and therefore is in the leakage flux of maximum density. In such a case the eddy losses may be three times the average eddy loss of the entire winding.

In the given example two radically different problems are involved:

1. To adjust the volume and velocity of oil flow in parallel ducts in order to equalize the temperature of the two coils having different watts per square inch of surface.

2. To equalize the temperature in two parts of the same coil having different thicknesses of turn insulation, by varying the oil velocity.

PARALLEL DUCTS

To compensate for the increase in watts per square inch due to the greater eddy loss, by means of an increase in oil velocity, the duct around the inner coil must be made correspondingly larger than the duct around the outer coil. The reason for this is that for any given oil pressure, not only the total oil flow in the duct, but also the velocity of oil flow increases with an increase in size of the duct. Figure 14 shows how much larger this inner duct must be than the outer duct in order to obtain the desired reduction in temperature drop across the surface film.

However, this method of balancing temperature by oil flow in parallel ducts is not always practicable because of the following considerations.

At the present time, to conserve copper in the directed forced-flow transformers, the watts per cubic inch of copper and watts per square inch of the winding surface exposed to oil are much greater than are used in self-cooled transformers. On the other hand, the thickness of solid insulation, such as turn insulation, is naturally the same in both self-cooled and forced-flow transformers. Therefore, the temperature drop, T_s (Figure 11) across the turn insulation in forced-cooled transformers is greater than in self-cooled in the ratio of watts per square inch used.

In forced-cooled transformers the temperature drop T_o across the oil film is reduced radically by the high velocity of the oil flow, and therefore, in spite of the high watts per square inch, this temperature drop may be even smaller than in the self-cooled transformer. On this account, T_s in directed forced-cooled transformers may be equal to or even greater than T_o instead of being a small fraction thereof, as commonly found in self-cooled transformers.

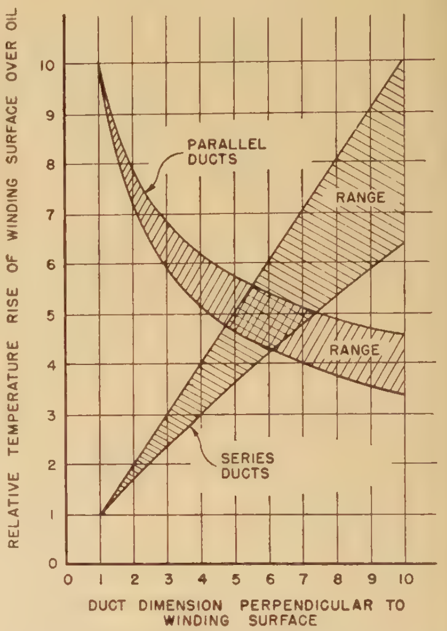


Figure 14. Relation between the size of oil ducts and temperature of the winding surface for a given watts per square inch

For example, let $T_s=T_o=7.5$ degrees centigrade. The winding rise over adjacent oil will be $T_w=T_s+T_o=15$ degrees centigrade. Assume that the inner coil (Figure 2) on account of greater eddy loss, has 20 per cent more watts per square inch than the outer coil. Its temperature rise over oil will be 15 degrees centigrade$\times 1.2=$ 18 degrees centigrade. To reduce the rise to 15 degrees centigrade, by increasing the oil flow in the duct, means that the temperature drop across the oil film must be reduced from 7.5 to 4.5 degrees centigrade because obviously the temperature drop through the insulation cannot be decreased. To accomplish this reduction, the duct around the inner coil must be made at least four times greater than the duct around the outer coil (Figure 14).

Such an increase in oil duct is entirely impractical, not only because additional copper and iron would be necessary to avoid increasing the reactance, but also because it would result in an inefficient use of oil flow.

Therefore, it is not practicable to equalize the temperature in two coils having greatly different watts per square inch, merely by the adjustment of the relative size of the parallel oil ducts.

Fortunately, in modern concentric-winding transformers the eddy losses generally are so small a percentage of the I^2R loss that no difficulty is encountered from this cause.

SERIES DUCTS

To compensate for the increase in temperature rise, due to the addition of extra end-turn insulation, the oil velocity over that section of the winding may be increased by *reducing* the size of the duct in accordance with curves given on Figure 14. This can be accomplished in several ways without affecting adversely either the amount of active materials or any of the transformer characteristics. Since the length of these smaller ducts will be a few per cent only of the total length of the coil, the increase in the resistance to the oil flow is insignificant.

In directed zigzag flow in concentric windings, as shown in Figure 15, the utmost flexibility exists in the control of the oil flow, which is both vertical and radial and is obtained by alternating fillers of insulation placed on the inside and outside of the disk coils. In this manner, the velocity and amount of oil and the pressure required can be varied widely without changing the winding. Whenever extra insulation is put between the copper and the oil stream, the ducts can be reduced correspondingly to increase the velocity and to compensate to some degree for the increased temperature drop across the solid insulation.

Because of the above structural characteristics of concentric windings, the effective use of directed flow of the oil does not require a departure from structures that have been used for a great many years.

From the preceding analysis it follows that the structural features of concentric-cylindrical as well as concentric-disk-type transformers naturally harmonize with the requirements for the effective use of directed-flow forced-cooling:

(*a*). The eddy losses are so small that they do not require disproportionately great increases in oil ducts.

(*b*). Increase in temperature rise through extra end-turn insulation can be compensated for by increase in oil velocity without waste of space or material.

(*c*). The oil flow is always parallel to the spacers, and, since the ducts are naturally uniform in cross section, the velocity of the oil may be uniform when desired.

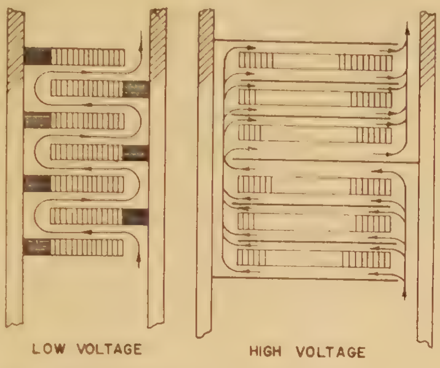


Figure 15. Directed zigzag oil flow through concentric windings

(*d*). The insulation structure is such that it does not interfere with the oil flow or with the division of oil between the windings.

Thus, it is interesting to observe once again the extraordinary structural harmony that exists in concentric-winding transformers. This harmony, as has been described to the Institute before, is the main cause behind the evolution of transformer structures from interleaved to concentric windings.[9]

VIII. Emergency Load

To prevent injury to directed-flow forced-cooled transformers, in case of an emergency created by an interruption of the power supply to the pump and fans, these directed-flow transformers may be designed to continue to carry 100 per cent load for one hour or more after the pumps and fans have stopped. The temperature rise of the winding hot spot at the end of this period will not exceed the limit recommended by the AIEE transformer subcommittee interim report, AIEE TRANSACTIONS, 1942 September section.[10,11]

IX. Conclusion

It is obvious that, with so many methods of cooling now available to the designer, a great variety of combinations of cooling methods is possible. Each one of these combinations has its own advantages under certain conditions of operation, but the greatest economy in space and materials is attained by the use of the directed forced-oil method of cooling. The over-all gain in materials used in the core and coils can be illustrated by the following simple example: The core and coils of a typical self-cooled, high-voltage 10,000-kva transformer, with 55 degrees centigrade average winding rise and 65 degrees centigrade hot-spot rise, when arranged for directed, forced oil flow, will carry continuously 16,667 kva, and meet the same temperature limits.

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Shunt Capacitors

AIEE COMMITTEE ON POWER TRANSMISSION AND DISTRIBUTION
General Systems Subcommittee

THE Institute at present is fostering a program encouraging discussion of means of obtaining greater loading out of existing equipment. As part of this program the general systems subcommittee of the power transmission and distribution committee has undertaken the task of analyzing what can be done with generators of reactive power in reducing the kilovolt-ampere demand upon system elements, thereby releasing more of the system capacity for useful kilowatt loading.

A discussion of this subject is greatly simplified by using the expressions kilovars, to refer to the reactive power and kilovolt-amperes, to refer to the product of volts and amperes. It is recommended that greater use be made of this distinction which has been recognized by the American Standards Association.

The breakdown of kilovolt-amperes into its two components, kilowatts and kilovars, is receiving greater and greater acceptance in the industry. Each component is being considered as a separate and distinct entity, even to the extent of not using necessarily the same direction of flow in the system. Thus an overexcited generator is usually considered as feeding both kilowatts and kilovars into the system. An overexcited synchronous motor is usually considered as taking kilowatts from the system but feeding kilovars into the system. In the former case, the directions of flow are the same, whereas in the latter case they are different. A shunt capacitor is thought of as feeding kilovars into the system. Thus, without attributing a sign to kilovars, the operator dispatches this quantity and provides for reversal of the quantity by the device of referring to the flow in or out at that point of the system. For some purposes it is desirable that the positive sense of flow of kilovars be in a certain direction. This makes it necessary that a distinction be made between a positive and a negative kilovar, and therefore a sign must be added before the number representing the kilovar. In order to be consistent, the foregoing type of kilovar will be regarded as positive, and, when occasion arises to reverse the flow of kilovar without reversing the positive sense of flow, a negative sign should be used. Thus an overexcited generator may be regarded as drawing negative kilovars from the system.

It is interesting to note that this conception of the positive kilovar is in direct conflict with the definition given by the American Standards Association.

While this report is concerned primarily with the use of capacitors as generators of kilovars, many of the conclusions drawn herein can be applied equally to other generators of kilovars, such as overexcited synchronous machines.

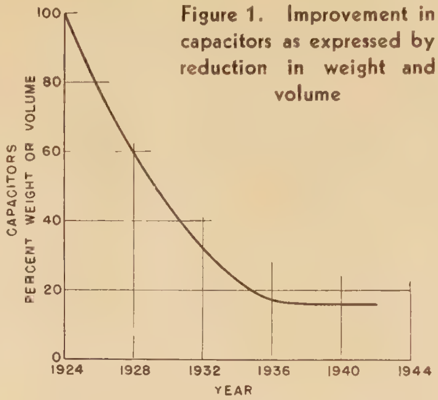

Figure 1. Improvement in capacitors as expressed by reduction in weight and volume

Development

Capacitors for power-factor correction first made their appearance as early as 1914. By 1928 the total kilovolt-amperes installed was approximately 260,000. At the end of 1938 the total had risen to 1,400,000 kva, and by the end of 1941 the estimated total installed capacity was 3,000,000 kva. In 1942 the total kilovolt-amperes of capacitors which will be produced has been estimated at 2,500,000, which would bring the total estimated installed capacity as of January 1, 1943, to about 5,500,000 kva.

Prior to 1932 all power capacitors were of the oil-type construction. The use of chlorinated aromatic hydrocarbon impregnating compounds and other advances in the art brought about sharp reductions in size and weight. Today, complete outdoor capacitor installations weigh five pounds or less per kilovolt-ampere, as contrasted with 15 or 20 pounds per kilovolt-ampere in 1925. Prior to 1937 practically all capacitors were installed in industrial plants. The development of outdoor-type units eliminating steel housings and other accessories resulted in a further increase in the use of capacitors.

The improvement in capacitor design is best reflected in the size and weight. Figure 1 illustrates the results of development in the period between 1924 and 1942. The general acceptance of capacitors has been due to the following:

1. Reduction in selling price.
2. Improvement in design and manufacturing methods resulting in decrease in size and weight.
3. Development of outdoor pole-type units and standardized mounting brackets.
4. Reduction in failures.
5. Better understanding of system benefits which accrue from their use, such as release of system kilowatt capacity and low loss as compared to other means of supplying kilovars.
6. The fact that they require the least critical material to supply kilovars.
7. The fact that, for the war emergency, manufacturing facilities are more available than for other means of supplying kilovars.

General Features

Capacitor banks are usually made up of standard 15 kva-units. A test voltage of somewhat over twice rated voltage is applied from line to line. The normal service voltage should not exceed 115 per cent of the rated voltage for standard voltage ratings of 575 volts and below and 110 per cent for standard voltage ratings above 575 volts. Capacitors should not be applied to circuit voltages in excess of five per cent of their rating. The line-to-ground test voltages vary with indoor or outdoor type and range from 43 times rated voltage for the 230-volt outdoor capacitor to twice for the 13,800-volt indoor capacitor.

PROTECTION

Capacitors connected to ground reduce the magnitude of surge voltages to ground that occur on systems and, with some risk, may not require arresters for their protection. Ungrounded capacitors should, however, be protected by arresters. Discharge devices are usually built into the capacitor, so that, when the capacitor is de-energized, any remaining charge is dissipated as a safety precaution.

Short-circuit protection is required primarily with capacitors for detecting and

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Personnel of general systems subcommittee: C. F. Wagner, chairman; J. W. Butler, S. B. Crary, J. E. Hobson, E. K. Huntington, A. A. Johnson, A. A. Kroneberg, M. C. Miller, H. K. Sels, H. M. Trueblood.

removing a faulty unit without causing other protective devices to operate, and for removing the unit before the internal arc generates sufficient pressure to burst the case which might cause additional damage. The requirements for wye-grounded and delta banks as contrasted with those of the wye-ungrounded banks, warrant a separate discussion for each.

For wye-grounded or delta banks a faulted capacitor unit requires that the fuse—individual or line—interrupts the full-system short-circuit current. Thus, on large systems, the fuse requirements can become very heavy, and, because of fuse limitations, these type banks have limitations.

For wye-ungrounded banks the current through a faulted unit is only three times the normal current of the capacitors connected to that particular phase and is neutral, since it is limited by the capacitors in the two sound phases. Thus, the current to be interrupted is sensibly independent of the system available short-circuit current, and the interrupting requirement of a fuse is under quite complete control since the bank is divided into sections. Thus, a system with large short-circuit requirements can have capacitors applied to it with low-capacity fuses of the individual or line type. Grouping several capacitors under a single line fuse offers many useful arrangements.

In these floating neutral equipments the short-circuit current generally can be reduced to such a value to allow circuit breakers controlled by relays to disconnect the bank before a faulty unit bursts. Three such schemes are:

1. A current transformer is placed in the neutral between the two groups to energize a current relay for initiating the tripping of the circuit breaker when objectionable unbalance occurs.

2. A voltage transformer is placed between the neutrals of the two groups to energize a voltage relay which initiates the tripping of the capacitor circuit breaker when objectionable unbalance occurs.

3. A voltage transformer is connected across each capacitor phase with the secondary windings in series with a voltage relay which initiates the tripping of the circuit breaker when objectionable unbalance occurs.

Harmonics

The impedance of a capacitor varies inversely as the applied frequency. Therefore, relatively small harmonic distortion in the applied-voltage wave form may produce very large current-wave distortion. To allow some harmonic voltage, the capacitors being built today, according to National Electrical Manufacturers Association standards, permit combined 60-cycle and harmonic kilovolt-amperes to equal not more than 135 per cent of the capacitor rating. To obtain the kilovolt-ampere loading at which a capacitor is operating, expressed as a fraction of its rating, with harmonic voltages E_3, E_5, E_7, and so forth applied, it is only necessary to insert the voltage values in the following expression:

$$\text{kva} = E_1^2 + 3E_3^2 + 5E_5^2 + \ldots\ldots \qquad (1)$$

where all voltages are expressed as a fraction of the rated voltage. If only one harmonic is present in addition to the fundamental, the foregoing rule limits this harmonic to the following:

$$E_n = \sqrt{\frac{1.35 - E_1^2}{n}} \qquad (2)$$

where n is the order of the harmonic.

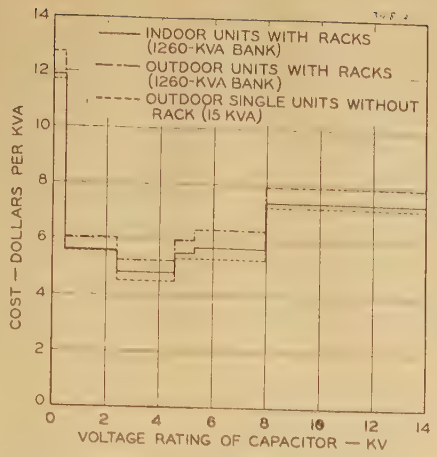


Figure 2. Cost of capacitors as a function of rated voltage

Use lowest cost figure indicated for all standard voltage classes

——— Indoor units with racks (1,260-kva bank)

— - — Outdoor units with racks (1,260-kva bank)

- - - - - Outdoor single units without rack (15 kva)

Losses and Ambient Temperature

The losses of present-day capacitors in kilowatts are about one third of one per cent of their kilovolt-ampere loading as determined by equation 1. The operating ambient temperature should not exceed certain figures which vary with the type and the application. For outdoor open mounted units this value is 50 degrees centigrade and for housed units between 40 and 50 degrees centigrade, depending upon the type of rack.

Costs

Costs of capacitors vary with the operating voltage, being higher at the low and high voltages and minimum at intermediate voltages. Figure 2 shows the variation of cost with voltage.

Application of Capacitors

The three principal benefits that accrue from the application of capacitors are:

1. Reduction in current for the same kilowatt load, or reduction in the required kilovolt-amperes of equipment for the same kilowatt load through the equipment.

2. Improvement in voltage level, and, if properly switched, improvement in regulation.

3. Reduction in system I^2r and I^2x losses

All of these benefits usually go hand in hand but have different limitations beyond which it is uneconomical to install additional capacitors. While certain benefits can be shown to result by reducing the current passing through isolated pieces of apparatus, the benefits are usually more far reaching. Thus, supplying the kilovars at or near the utilization point not only reduces the current in the distribution circuit feeding the load at that point, but the effects are reflected all the way back to the generator in that the current, the system voltage drop, the I^2r and the I^2x losses are less throughout the entire system. In the following discussion consideration will be given first to the effect of capacitors on isolated equipment, and this will be followed by consideration of how the entire system is affected. These discussions are very closely identified with system economics.

Current Limitation

When the load on a transformer exceeds its rating, two alternatives present themselves:

1. Additional transformer capacity can be added.

2. Capacitors can be connected in parallel with the load, until the current in the transformer or the vector sum of the kilovolt-amperes of the load and the capacitor falls within the rating of the transformer, provided, of course, the kilowatts of the load do not exceed the kilovolt-amperes of the transformer.

Naturally the proper choice depends upon transformer and capacitor cost and upon the amount of capacitors required.

Figure 3 illustrates how the application of capacitors increases the amount of real power that can be carried by a transformer. In Figure 3a is shown a transformer of unity kilovolt-amperes supplying a load of 80 per cent power factor. Assume first that the capacitor is not connected. From Figure 3b it can be seen that only 0.8-kw load can be supplied without exceeding the rating of the transformer. If the load is increased to 0.9 kw, and the same power factor is maintained, then the kilovolt-amperes of the load is 0.9/0.8 or 1.125, and the kilovar of the load is (0.9/0.8) 0.6 or 0.675. To pass a load of 0.9 kw through the transformer and still keep within its rating, the kilovar as shown by the circle, must be limited to 0.435. This can be accomplished by placing a capacitor across the load which will consume the difference between 0.675 kilovar and 0.435 kilovar or 0.24 kilovar. The useful load has thus been increased from 0.8 to 0.9 kw or 0.1 kw at the expense of the addition of a 0.24-kva capacitor.

The curves in Figure 4c express more generally the same results and show the total kilowatts that can be passed through the transformer as a function of the kilovolt-amperes of the capacitor connected across the load. The transformer is kept loaded at rated kilovolt-amperes at all times. The curve in Figure 4d gives the increase in kilowatt load as a function of the capacitor kilovolt-amperes and the curve of Figure 4e the increase in load kilovolt-amperes. The curve in Figure 4f, on the other hand, obtained by dividing the abscissa of curves of Figure 4e by the corresponding ordinate, gives the capacitor kilovolt-amperes per increase of load kilovolt-amperes again plotted against capacitor kilovolt-amperes required. This curve is of particular interest, since when one multiplies this quantity by the cost of the capacitor per kilovolt-ampere, the product gives the cost of supplying an additional kilovolt-ampere of load. This cost, neglecting other advantages of the capacitor, should be compared with the cost per kilovolt-ampere of increasing the transformer rating. Thus, if the load is 70 per cent power factor, and a capacitor kilovolt-ampere of 0.4 is added, the capacitor kilovolt-amperes per increase in kilovolt-amperes of the load is 1.65. If the capacitor cost is $7.00 per kilovolt-ampere, then the increase in ability to supply load is obtained at a cost of 1.65 times $7.00 or $11.55 per kilovolt-ampere. If the transformer cost per kilovolt-ampere is $10.00, then considering the other advantages of the capacitor, the latter may be more economical.

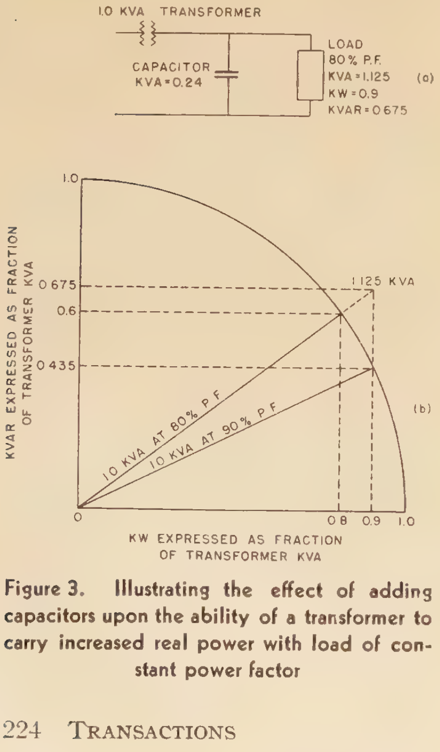


Figure 3. Illustrating the effect of adding capacitors upon the ability of a transformer to carry increased real power with load of constant power factor

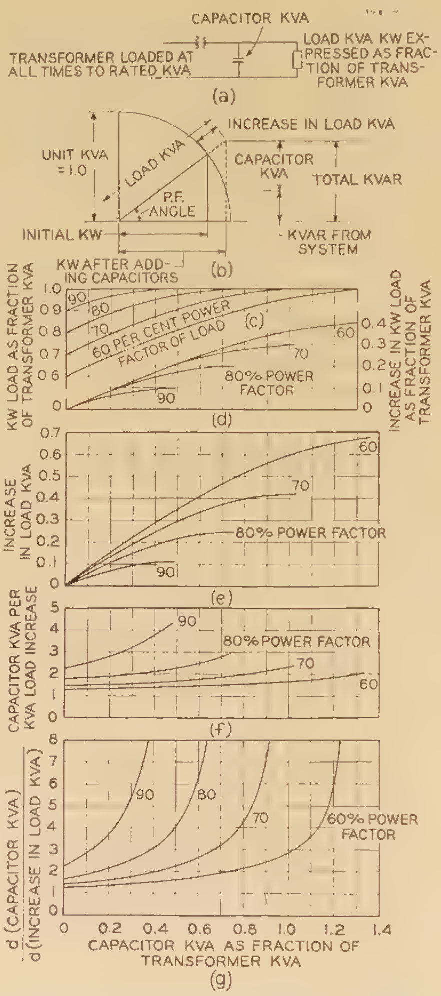


Figure 4. Effect of shunt capacitors upon loading of a transformer

From Figure 4b it is apparent that, as the load through the transformer approaches unity power factor, smaller and smaller incremental gains in load are obtained for incremental increases in capacitor kilovolt-amperes. Figure 4g gives the incremental capacitor kilovolt-amperes required for an increment in kilovolt-amperes of the load. Expressed mathematically, the ordinate in this curve is equal to

$$\frac{d\,(\text{capacitor kva})}{d\,(\text{increase in load kva})}$$

This curve shows that the final increment is attained at much greater expense than the initial increment.

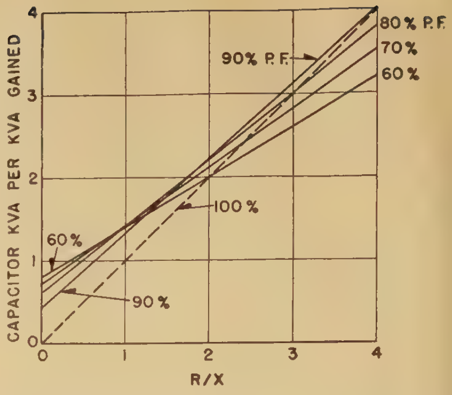


Figure 5. Gain in load kilovolt-amperes by adding capacitors, maintaining constant voltage drop

In the foregoing a transformer was used for illustrative purposes only. The same arguments apply equally well to any equipment in which current might constitute a limiting factor such as generators, cables, regulators, and occasionally overhead transmission and distribution lines. Consider for a moment a generator. Modern 0.8 and 0.9 power-factor generators can, in general, be operated at full kilovolt-amperes at unity power factor. In most cases the turbine is capable of delivering this increased power above its rating. However, there may be other elements in the electric and steam systems that would make it undesirable to operate under these conditions. The power generation aspects of the problem are being presented in a symposium on this subject by the power generation committee. The present shortage of critical materials for the construction of generators makes this question particularly important at this time. Since the prime function of generators is to deliver kilowatts, they may, where additional power is needed, be retained for this purpose and the required kilovars supplied by other means at or near the load. In applying the cost data obtained through the use of the curves of Figures 4f and 4g, it is necessary in this case to compare capacitor costs with generator costs. In almost all cases it will be found that capacitors, to bring the operating power factor of the generator to near unity, will be justified. Other considerations, such as stability of the generator at low excitation, might dictate the extent to which the power factor can be corrected.

As will be shown subsequently, additional advantages, such as reduction in losses and improvement in regulation, can be obtained by locating the capacitor close to the load. Since the system investment increases per kilovolt-ampere delivered as the load is approached, and since capacitors decrease the required system kilovolt-amperes per kilovolt-ampere delivered, it becomes easier to justify the application of capacitors at points closest to the load.

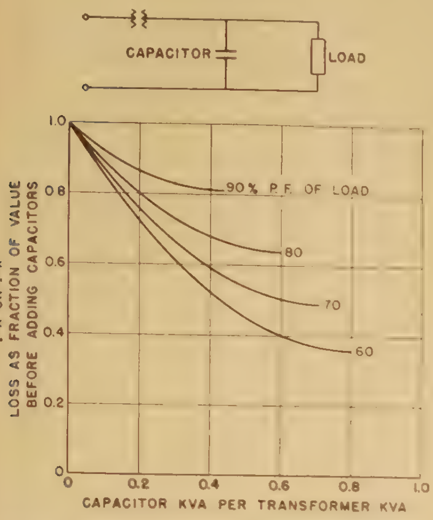


Figure 6. Effect of adding capacitors upon the loss in a feeder, transformer, or other equipment supplying a load

Voltage Drop

The voltage drop in feeders on short lines can be expressed approximately by the relation

$$\text{Voltage drop} = RI_r + XI_x \qquad (3)$$

where R is the resistance, X the reactance, I_r the power component of the current, and I_x the reactive component. If a capacitor is placed in shunt across the end of the line, the drop becomes approximately

$$\text{Voltage drop} = RI_r + XI_x - XI_c \qquad (4)$$

where I_c is the current drawn by the capacitor. Thus it is evident that if I_c be made sufficiently large, both the RI_r and the XI_x drops can be neutralized. This expression also shows that, if the voltage drop is compensated at full load, then at light loads, as I_r and I_x become smaller, the line is overcompensated, because I_c is dependent only upon the voltage and not upon the load. Thus it may be said that the regulation of the line is practically unchanged by the presence of the capacitor. The capacitor effects an increase in voltage both at light load and at full load. At light loads the voltage rise might well be so much in excess of the normal value as to represent an undesirable or even intolerable condition.

Because of its lack of voltage control, a permanently connected capacitor has limitations. However, with suitable control for switching capacitor banks or sections of banks in and out, the capacitor can be made to control the voltage. The control element can be operated either by a time switch, from the line voltage, by the load power factor or by the load kilovars. To date the voltage control has received greatest acceptance, because in general, it affects directly the quantity which it is desired to control, although in industrial applications power factor and load kilovars have been used quite frequently. Switching presents new problems unique in capacitor applications which, however, are susceptible to ready solution. These are:

1. When a capacitor bank is connected to a system, the system impedance is usually of sufficient magnitude to limit the initial inrush to a reasonable value. If, however, a section of a bank is switched in while another section of an adjacent bank is already connected to the system, the low impedance between the sections can result in large currents of high frequency. Experience has indicated that this need not be given consideration in the general case.

2. Opening of the capacitor charging current—under the proper conditions—can result sometimes in restriking of the arc within the breaker, with resultant high transient overvoltages. This is essentially a switching problem and can be handled adequately with properly applied available switching equipment.

The switches need not, however, have sufficient interrupting capacity to handle the system short-circuit current. Breakers are usually supplied for this purpose which can also be used as disconnecting devices, providing there is a backup breaker. For outdoor service suitable fuses are also used for this purpose. In industrial applications capacitors are sometimes connected directly across the terminals of a motor and switched on and off with it.

The number of steps to be used in capacitor banks depends entirely upon the system characteristics, the total drop which it is desired to correct, and the increment in voltage change which the system can absorb. In this respect the switched capacitor is in a position similar to the step voltage regulator as contrasted to the continuous variation of the induction-type regulator.

To apply capacitors most effectively it is desirable that wattmeter, kilovar meter and voltmeter records be taken at several points throughout the system. These will serve as a guide to determine the amount of capacitors that should be installed and also the necessity for and number of steps in switching equipment.

To compare the benefits of line-drop compensation by means of capacitors with other methods of accomplishing the same result, the curves shown in Figure 5 are provided. These curves show the capacitor kilovolt-amperes required per kilovolt-ampere increase in load that can be supplied without increasing the line drop. It will be observed that this quantity is a function only of the power factor of the load and ratio of resistance to reactance of the feeder, transformer, or other device. By multiplying the ordinate by the cost of a kilovolt-ampere of installed capacitor, the cost of supplying an additional kilovolt-ampere of load without increasing the drop is obtained. This figure should be compared with the cost to produce the same result by other means, such as additional cost per kilovolt-ampere of transformer or feeder.

Reduction in Losses

When current flows through a circuit consisting of resistance and reactance, there is in addition to the I^2r loss an I^2x loss. The generator must, therefore, in addition to supplying the load requirements, supply the I^2r loss in the form of kilowatts and the I^2x loss in the form of kilovars. If the current through the system is decreased, then the I^2r and I^2x losses which the generator must supply will decrease. In the case of reduced I^2r losses, the useful kilowatt loading of the generator can be increased by practically this amount without requiring more fuel. The smaller I^2x losses reduce the kilovolt-ampere loading on the generator and thereby release generator current-carrying capacity which may be used to supply kilowatts.

Under the present emergency, reduction in kilowatts (I^2r) and kilovars (I^2x) losses has taken on new importance and significance. So long as the system has adequate reserve, there is some justification for those who argue that the kilowatt losses should be evaluated in terms of the fuel required. However, as the reserves become depleted, and the system is operated closer and closer to its theoretical capacity, losses must be evaluated in terms of both the total equipment and the fuel required. Under present conditions when generating capacity is difficult if not impossible to obtain, reduction in losses becomes still more important.

Capacitors are effective in reducing

the current and, consequently, the I^2r and I^2x losses. Of course, in the case of a transformer or a feeder in which the current-carrying capacity is limited by thermal consideration, this factor is only another facet of the same problem. When capacitors are installed near the load, the reduction in current affects the loss throughout the entire system. Calculations of reduction in loss under peak load conditions have shown that capacitors of reasonable amounts, in addition to other benefits, might also reduce the system generator output by the extent of two to four per cent. Figure 6 shows the reduction both in I^2r and I^2x losses that result by improving the power factor of the total load on a transformer or feeder by the addition of capacitors, maintaining all the while the same load.

Over-All System Performance

While the foregoing discussion illustrates the advantages of the application of capacitors from the standpoint of isolated equipment, the full advantages of

Figure 7. System performance with application of shunt capacitors

Base load at each point *C* (load center of single-phase laterals)
At time of peak load—100 kw 75 kilovars 125 kva at 80.0 per cent power factor
At time of light load—20 kw 30 kilovars 36 kva at 55.5 per cent power factor

Total load at points C for 15 circuits
At time of peak load—13,500 kw 10,125 kilovars 16,875 kva at 80.0 per cent power factor
At time of light load—2,700 kw 4,050 kilovars 4,868 kva at 55.5 per cent power factor

Base load on each 240-volt secondary line
At time of peak load—7.83 kw 5.68 kilovars 9.67 kva at 80.9 per cent power factor
At time of light load—1.59 kw 2.29 kilovars 2.79 kva at 57.0 per cent power factor

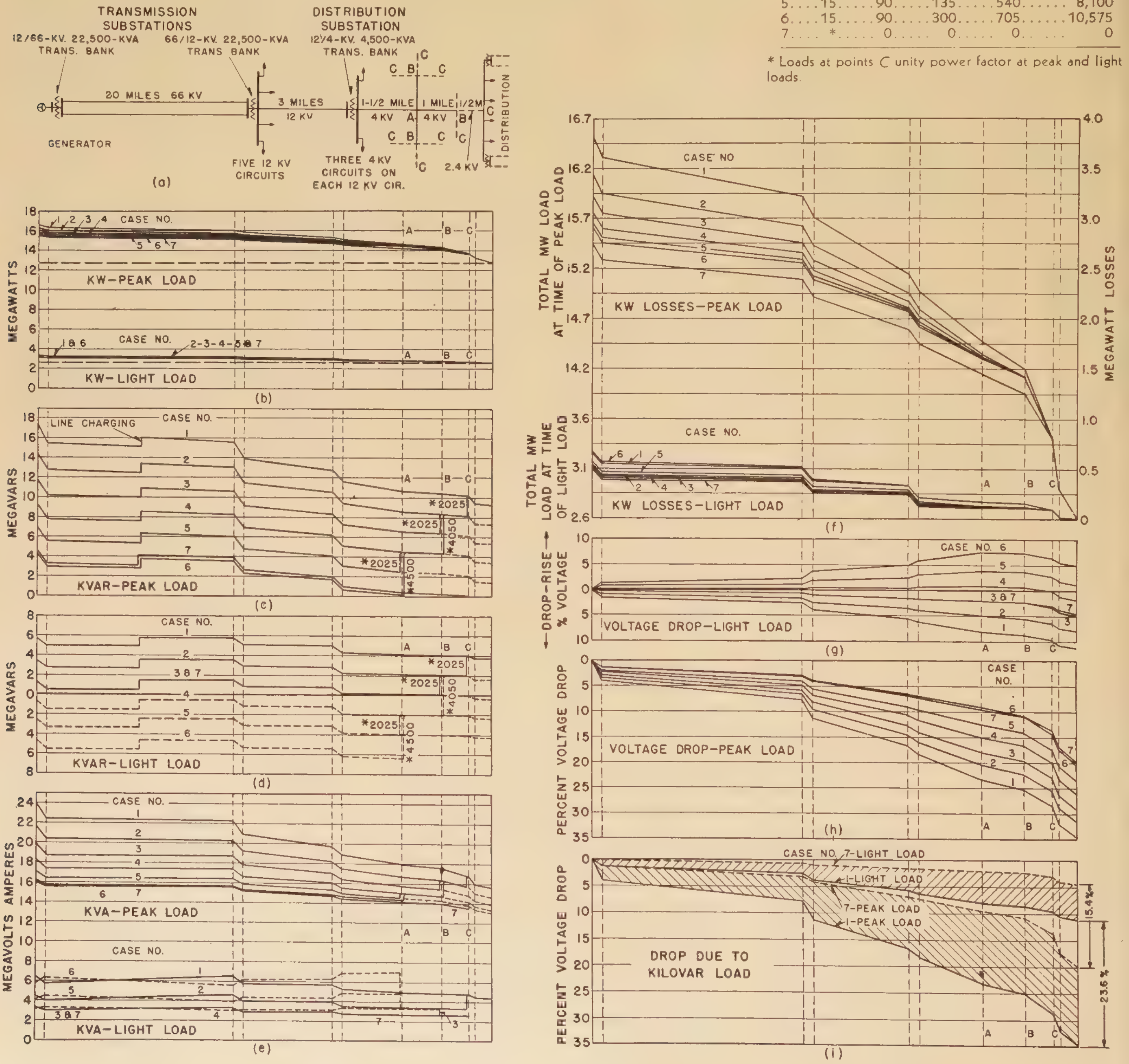
Total load on secondary lines
At time of peak load—12,683 kw 9,208 kilovars 15,670 kva at 80.9 per cent power factor
At time of light load—2,578 kw 3,707 kilovars 4,526 kva at 57.0 per cent power factor

Cases Assumed for Calculations

Kilovars of Capacitors on Each of 15 Circuits

Case No.	At *C* (×9)	At *B* (×3)	At *A* (×1)	Total Per 4-Kv Circuit	Total on 15 Circuits
1	0	0	0	0	0
2	15	0	0	135	2,025
3	15	45	0	270	4,050
4	15	90	0	405	6,075
5	15	90	135	540	8,100
6	15	90	300	705	10,575
7	*	0	0	0	0

* Loads at points *C* unity power factor at peak and light loads.



the capacitor cannot be appreciated without analysis of the entire system. For any particular system this analysis can be made best on a network calculator or a network analyzer. In this case, the capacitors can be spotted throughout the system and the net result determined readily and quickly for both full-load and light-load conditions. To illustrate the nature of these advantages, M. C. Miller has chosen a hypothetical system which is sufficiently simple to analyze mathematically. The results of this analysis were presented before the Edison Electric Institute, and a portion of the paper is included in this report. The chosen system is shown in Figure 7a. Briefly, it comprises a generator, step-up transformer to 66-kv transmission, a step-down transformer to five 12-kv circuits, each serving a 12-kv to 4-kv substation with three 4-kv distribution circuits. This makes a total of fifteen 4-kv distribution circuits. Emanating from the terminals of these three-phase circuits indicated by the point *B* are 2.4-kv single-phase laterals of which there are nine for each 4-kv three-phase feeder. Power is stepped down on these lines to 120/240-volt utilization voltage. The loadings for peak and light loading conditions are also given in Figure 7a. Installations of shunt capacitors throughout the system were assumed, which are identified by the case numbers assigned to them in the table of Figure 7a. Cases 1 and 7 assume the same kilowatt loading with no capacitors installed; the loads at the points *C* for the former being at 80 per cent power factor and for the latter at unity power factor.

The curves in Figure 7 are self-explanatory, as they depict the kilowatt, kilovar, kilovolt-ampere, and voltage at different points throughout the system for both peak-load and light-load conditions. A number of details are worthy of special comment.

The kilowatt increases as the generator is approached, a fact shown in magnified form by the loss curves in Figure 7f. For a customer load of 12,700 kw with no capacitors, the generator output is 16,500 kw, representing a system loss of 3,800 kw or 30 per cent. Of course, the larger portion of this loss is unavoidable and represents a loss resulting from the transmission of real power only. Note that for case 7 for the peak load the kilovar is very small, and for practical purposes the losses for this case represent the ultimate to which they can be reduced; that is, they correspond to within one tenth of a per cent with the losses that would result if the kilovars were supplied at the points of origin by capacitors distributed along the lines.

However, the amount by which the losses are reduced is still quite substantial. Thus the first increment of capacitors (2,025 kilovars) reduces the losses by 360 kw. The effects upon the kilovolt-ampere output of the generator are very much greater. The first increment of capacitors (2,025 kilovars) at the load reduces the kilovolt-amperes (not kilovars) by 2,200 kva at the generator. Figure 8 shows the effect of additional increments of capacitor kilovars upon both the kilowatt losses and amount of generator kilovolt-amperes released. The points should not necessarily form continuous curves, because they represent increments of capacitors at different locations throughout the system. The difference in the losses for cases 6 and 7 represent the benefits, as can be seen from Figure 7f, of locating capacitors at *C* instead of *B*, thus decreasing the losses in the circuit between these two points. Even under normal national economic conditions it appears desirable to relieve the generator of the duty of supplying a portion of the kilovars and to transfer that function to the capacitor. Under present conditions, with the increasing difficulty of obtaining fuel, the reduction in losses becomes even more important. The restriction in the availability of generating equipment is fortunately relieved to a very considerable extent by apparatus which is relatively low in cost and has few restrictions as to availability. Considering the generating capacity of the entire country, which is something in excess of 25,000,000 kw, it may be seen that releasing even as small an amount as two per cent makes available 500,000 kw of generating capacity. Of course, the foregoing considerations are predicated upon the ability of the generator to supply full kilovolt-amperes at higher power factor than rated, which is not always the case. To a lesser extent, similar considerations permit the installation of smaller transformers if the power factor is corrected by capacitors.

Miller's curves illustrate also what he terms the "snowball" or cumulative effect upon the reduction in kilovars by locating the capacitors as close as possible to the load. To produce the same reduction in kilovars at the generator as obtained by 2,025 kilovars of capacitors at the load would require 3,000 kilovars of capacitors connected directly across the terminals of the generator. This constitutes a further argument for placing capacitors near the load.

In applying capacitors, consideration should be given not only to the peak-load condition but also to the light-load condition. If the capacitors are not switched off at light loads, the disappearance of the lagging kilovars produces an excess of loading kilovars. This produces two results:

1. The generator becomes underexcited and this may be dangerous if the system has a tendency toward instability.
2. The voltage rises near the load end of the system.

Provision, of course, can be made to switch capacitors off automatically at times of light load. Figures 7g and 7h illustrate the variation of voltage between peak load and light load for the different cases just discussed. A voltage drop in a system is not so serious if it is the same at peak and light loads, since the voltage level can be corrected by merely changing a transformer tap. In the particular system under consideration it will be observed that a voltage drop of 35 per cent occurs at the time of peak load and 11 per cent at the time of light load. Therefore, the voltage regulation itself is 24 per cent, a magnitude which would be intolerable and must be corrected either by switching of capacitors, tap changing under load, or induction regulators. If, however, at peak load and light load the kilovar component of the load is compensated by the installation of capacitors (case 7), the regulation is only 15 per cent. This of course, involves switching of the capacitors between full load and light load. Verification of these values can be obtained from Figures 7g, h, and i.

Idle generators are sometimes floated on the system to supply kilovars. This has the disadvantage that the kilovars are not usually supplied at the most advantageous location and is not available when needed most, that is, at peak loads when the generator is delivering real kilowatts.

Comparison Between Capacitors and Synchronous Condensers

In large units synchronous condensers constitute a competitor of the capacitor and may be more desirable when all factors are evaluated. The following points should be considered in comparing these two types of equipment:

1. A standard synchronous condenser is capable of supplying kilovars equal to its rating to the system as well as of absorbing them to an extent equal to 50 per cent of its rating. For those applications requiring these characteristics, the comparison should be on a basis of the synchronous condenser

against the capacitor at full kilovars plus a shunt reactor of 50 per cent kilovars.

2. The fineness of control of the synchronous condenser cannot be duplicated by the capacitor unless a large number of switching steps are used.

3. An instantaneous drop in terminal voltage, within practical limits, increases the kilovars supplied to the system in the case of a synchronous condenser, whereas a similar change in the case of capacitors decreases the kilovars supplied to the system. In this regard the synchronous condenser has greater stabilizing effect upon system voltages and likewise tends to maintain synchronism between machines. Its mechanical inertia, in general, has a further stabilizing effect upon the other synchronous machines comprising the system. By reason of these same characteristics, a synchronous condenser reduces the effects of sudden load changes or rapidly varying loads, such as drop in system voltage occasioned by starting of a large motor or operation of large welders.

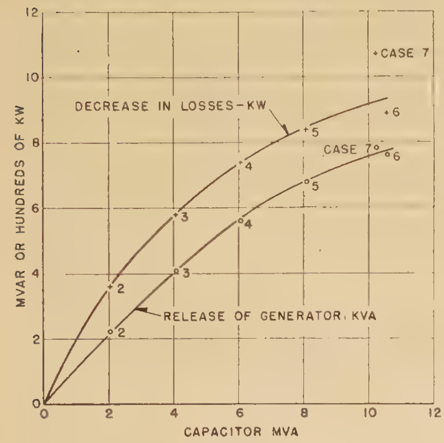


Figure 8. Reduction in system losses and reduction in kilovolt-ampere loading of the generator resulting from the addition of capacitors to the system of Figure 7

System before addition of capacitors represented by Case 1

4. For short periods of time the synchronous condenser can supply kilovars in excess of its rating at normal voltage, whereas this is not the case for capacitors.

5. The losses of synchronous condensers are very much greater than those of capacitors. For synchronous condensers the full-load losses vary from about 3 per cent for 3,000-kva units to about $1^1/_2$ per cent for very large units of 50,000 to 100,000 kva, and for capacitors the full-load losses are about one third of one per cent. The no-load losses of air-cooled synchronous condensers are about 60 per cent of the full-load losses, and for hydrogen-cooled synchronous condensers about 40 per cent; therefore, at fractional loads the losses of the synchronous condenser are not in proportion to the output in kilovolt-amperes. For a capacitor, however, the losses are proportional to the kilovolt-amperes connected to the system. Thus, the greater the load factor, the greater the benefits of the comparison of losses in favor of capacitors.

6. A comparison of the cost of synchronous condensers and capacitors involves an evaluation of the losses. Figure 9 compares the cost of air-cooled outdoor synchronous condensers and capacitors. Three evaluations for losses were assumed 0, $150, and $250 per kilowatt. The low losses of the capacitors should not be evaluated so highly as those for the synchronous condenser, because, as just mentioned, at fractional loads the losses decrease more rapidly than for the synchronous condenser. Figure 10 brings out the comparison still more clearly in that the ordinate is plotted as a ratio of the cost of the capacitor to the cost of the synchronous condenser. When losses are evaluated at $150 per kilowatt, the costs are equal at 35,000 kva, and, when evaluated at $250 per kilowatt, they balance somewhere around 100,000 kva. The balancing point between capacitors and synchronous condensers will vary with voltage and with the type of synchronous condenser.

7. Capacitors lend themselves to distribution throughout the system at several locations much more economically than synchronous condensers. Thus they may be located at points closer to the load and be more effective in their purpose.

8. The kilovolt-ampere rating of an installation can be increased or decreased as the loads and system requirements dictate much more readily with capacitors than with synchronous condensers and can be installed with greater ease. By moving capacitors from point to point as required, the installation of other equipment such as transformers may be deferred. Foundations are less important, and auxiliaries are fewer.

9. A failure of a single unit comprising a bank of capacitors affects only that unit and does not jeopardize operation of the entire bank. On the other hand, failure of insulation in a synchronous condenser is less likely to occur than failure of a single unit in a bank of capacitors.

10. Synchronous condensers add to the short-circuit current of a system and may increase the size of breaker required. On the other hand, switching of large banks of capacitors may involve large currents. In general, however, these currents fall within the ratings of circuit breakers.

11. During the present emergency critical materials require consideration. Synchronous condensers require between two and three times the critical material required by capacitors. There is the further consideration that the manufacturing space ordinarily used for synchronous condensers is now being used for the manufacture of equipment having higher priority rating. Thus, under present conditions, comparison between synchronous condensers and capacitors may be purely academic because of the difficulty of obtaining synchronous condensers.

Harmonic and Coordination With Telephone Circuits

The principal cause of harmonic voltages and currents in capacitors is the magnetizing requirements of transformers. Because of the lower impedance of capacitors at higher frequencies, the harmonic currents may become so high as to endanger the life of the capacitor or to cause excessive amounts of fuse blowing or overheating of breakers and switches. The standard margins built into capacitors, which were mentioned previously, are usually sufficient so that, for the amount of harmonic voltage present in most systems, no undue amount of trouble is experienced. For the transformer magnetizing current, the third-harmonic components and their multiples are usually supplied by circulation around the delta-connected windings. The higher harmonics are usually so small that they give no appreciable trouble so long as the transformers are operated near their rated voltage.

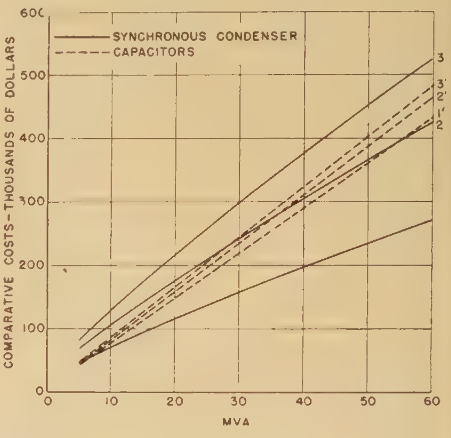


Figure 9. Air-cooled synchronous condenser and capacitor costs for a 13.8-kv outdoor installation

Capacitors connected in wye and switched in five steps. Costs do not include main circuit breaker, land space, foundations, or spare parts, but do include freight, automatic control erection, capacitor fuses, coolers on synchronous condenser, and so forth

1–1′—Cost of equipment installed

2–2′—Cost plus losses capitalized at $150 per kilowatt

3–3′—Cost plus losses capitalized at $250 per kilowatt

An unbalanced fault on a system supplied by water-wheel generators without damper windings may produce harmonic voltages. By resonance or partial resonance with capacitors, these voltages can be magnified. While the duration of the fault might not be sufficiently long to injure the capacitor, it may result in blowing of a large number of fuses in connected capacitors all over the system. This hazard is reduced by properly designed damper windings and system arrangement.

A considerable amount of study has been given to the effects of shunt capacitors on the inductive co-ordination of power systems and exposed telephone circuits at noise frequencies. These studies have been carried on by the Joint Subcommittee on Development and Research of the Edison Electric Institute and Bell Telephone System. The results of their preliminary study of the problem were included in an article published in the August 1938 issue of the Edison Electric Institute *Bulletin*. Subsequently, the joint subcommittee has co-operated with the operating power and telephone companies in a number of field studies on the effect of capacitors, and a report summarizing this experience is now in preparation. It has been found that the use of capacitors may be either detrimental or beneficial from the inductive-co-ordination standpoint, depending on the particular conditions in each case. Advance planning by the two industries along the lines suggested in the article referred to has reduced the number of troublesome situations to a small percentage of the number of capacitor installations. Where the use of capacitors has resulted in increased noise, it has been practicable generally to improve conditions by relatively simple measures applied to either the power or telephone systems or both. A summary of the available measures is included in the article.

Portable Capacitors

Several utilities have found, with a sudden increase in load at particular points on the system, a situation which is likely to occur under present conditions; portable three-phase capacitor units dispatched to the point are effective in relieving the load on the overloaded facilities until more permanent changes in the system can be made. Also, two single-phase mobile capacitor units are in use; their purpose is to relieve the overload on open-delta banks of transformers occasioned by the failure of one transformer of a three-phase delta-connected bank.

Self-Excitation of Induction Motors

In some capacitor applications the system is so arranged that, in the event of opening of a feeder circuit breaker, an induction motor paralleled with a capacitor may be isolated from the remainder of the system. The stored energy in the rotor and load will then drive the motor, and, if the kilovolt-amperes of the capacitor is sufficiently large, the combination will become self-excited, the motor acting as an induction generator receiving its excitation from the capacitors. The overvoltage is rarely of sufficient magnitude to endanger the insulation. If lights are connected to the same circuit, their life may be shortened somewhat because of the magnitude and duration of the overvoltage. These situations, however, rarely occur.

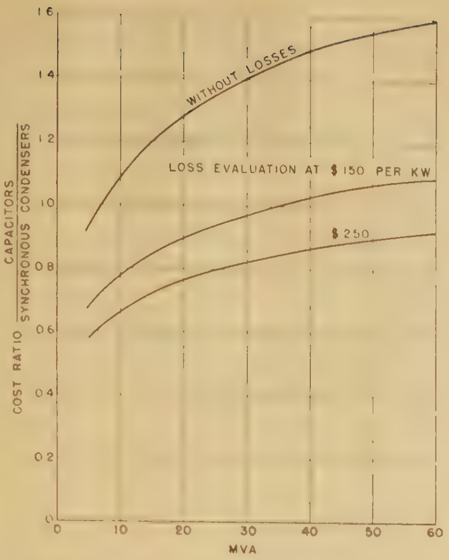


Figure 10. Relative costs of capacitors and synchronous condensers for conditions enumerated in Figure 9

Conclusions

1. Capacitors are usually the most economical form of supplying basic kilovars when installed near the load.

2. Capacitors release transformer, feeder, substation, or generator capacity, or stated otherwise, permit greater loading in terms of active or real power. This is particularly important during the war emergency.

3. Unless switching is used, the amount of capacitors that should be installed may be limited by

(*a*). Overvoltage conditions at light load.

(*b*). Synchronous machine instability.

(*c*). Over-all system energy losses.

4. The addition of capacitors reduces the system losses by supplying the load kilovar component. The greatest economy is obtained by supplying the kilovars close to the load. Reduction in total system losses reduces the fuel required to supply the load and also makes available additional generating capacity.

5. Capacitors require less critical material than synchronous condensers and do not tie up manufacturing space now required for equipment more essential to the war effort.

6. Permanently connected capacitors reduce voltage drop but have only a minor effect upon the regulation, since they raise the voltage at light load and at full load by substantially the same amount.

7. Capacitors, when located close to the load, release more kilovars in the synchronous machines than is indicated by the capacitor name-plate rating.

8. A capacitor does not have the favorable characteristic of a synchronous condenser in reducing light flicker.

9. The initial cost of capacitors and synchronous condensers balance around 7,000 kva, but, when the greater losses of the synchronous condenser are taken into consideration, the balance point will be very much greater. In this comparison it is necessary in some cases to recall that a standard synchronous condenser, if required, can also absorb kilovars from the system to an extent equal to 50 per cent of its rating.

10. Capacitors reduce billing charges, a factor of great importance to industrial customers having a power-factor clause in their power contracts.

11. Capacitors are readily made portable to provide emergency capacity where needed.

12. The proper application of capacitors requires a consideration of all components of the system, including generation, transmission, and distribution.

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The Cathode-Ray Oscillograph Applied to Long-Time Switching Transients

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Synopsis: The cathode-ray oscillograph because of its inherent characteristics is ideally suited and extensively used for the measurement of transient voltages associated with switching operations. The type of oscillograph ordinarily used covers a wide range of measurement conditions and is necessarily large and expensive. For this reason very few such units are available, and new problems, arising from the necessity of providing reliable service with loads increasing because of the war effort, have required every available measurement facility. In two recent applications where extremely wide-range performance was not required, a two-tube low-voltage cathode-ray oscillograph was used successfully. This oscillograph and the two applications are described, and it is shown how such measurements of long-time transients may be made with portable, inexpensive, and relatively simple equipment embodying the advantages of cathode-ray oscillography.

SINCE it first began to be appreciated that the transient voltages associated with the opening or closing of an electric-power circuit had an important bearing on the switching device, as well as the connected circuit elements, much thought and effort have gone into the development of instruments which will measure these transients faithfully.[1-3] This development has led to the modern cathode-ray oscillograph which tells the complete story of the transient both as to time and amplitude.

This instrument is suited ideally to this task because of certain inherent characteristics:

1. Its indicating element, a beam of electrons, has inertialess response to the rapid changes of the transient voltage.

2. It may be constructed so that its beam intensity is adequate to give a photographic record of those changes.

3. It may be either a voltage-sensitive or a current-sensitive device, but as the former it is capable of giving a measurement with extremely low energy drain from the circuit being measured. This means that it may be used with high-impedance-capacitance voltage dividers for the measurement of extremely high voltages at transient frequencies where potential transformers, even though practical from a voltage standpoint, would not respond accurately to the rates of change involved.

4. Its beam may be turned on or off with such speed and precision that recording may be readily synchronized with the occurrence of the transient, even to the extent of having the transient itself initiate the recording sequence.

Cathode-ray oscillographs have been designed and built and are in daily use in switchgear laboratories that are capable of recording, for all three phases, many cycles of power-frequency voltage, while at the same time adequately resolving transient oscillations of the highest frequencies encountered in this type of phenomena. Such units designed to cover the full range of measurement possibility are necessarily complicated and expensive, and, while they can be made mobile, they scarcely would be considered as portable. However, this full range of measurement capability is not always necessary. In fact, it is frequently desirable to sacrifice high-speed time resolution in order to get a better picture of the relation between various parts of long-time transient phenomena.

While this, of course, is readily accomplished with the big high-speed oscillograph, only a few such outfits are available, and these just about cover normal measurement needs. With the increased loading of electrical systems because of the war effort, many new engineering problems are constantly arising. The solution of these problems frequently requires both laboratory and field investigations to supplement past experience, and additional measurement facilities must be provided.

It is apparent that applications involving long-time transients may be handled by a cathode-ray oscillograph which, without the high-speed features of the all-purpose type mentioned above, could be made far simpler and less expensive. In the following pages such a simple, inexpensive outfit is described along with two applications to switchgear field testing.

The Oscillograph

This special cathode-ray oscillograph shown in Figure 1 was designed to record long-time transients with durations up to one or two seconds. For this reason it can use a low-voltage cathode-ray tube, and the unit incorporates two such tubes. Recording is by moving film photography.

(a). Tubes

The two cathode-ray tubes which are operated at 1,300 volts have three-inch fluorescent screens of the high-peak-intensity short-persistance type suitable for moving film photography. A view from above the oscillograph looking down into the tube compartment as in Figure 2 shows one tube in full but only the white screen of the second tube which is mounted directly underneath the first one. The voltages to be measured are applied to the vertical deflection plates,

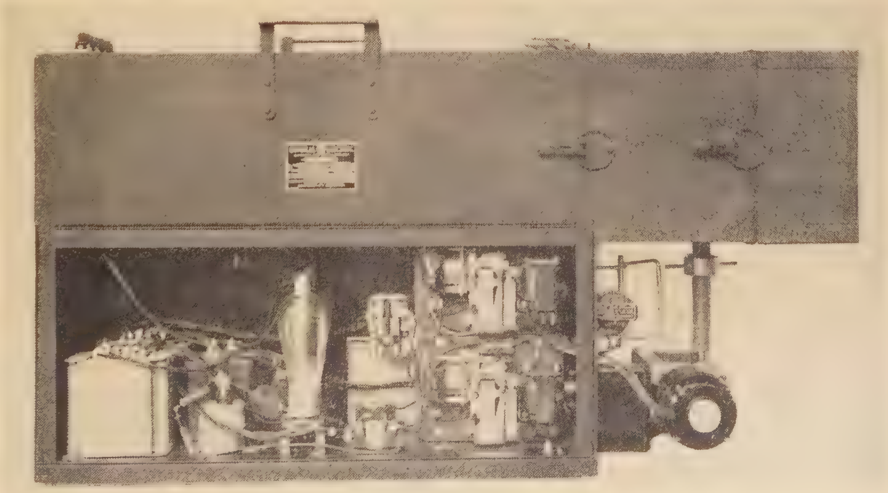

Figure 1. Special cathode-ray oscillograph

Side panel removed showing power-circuit and film-shifting relays. Film drum and drive in position

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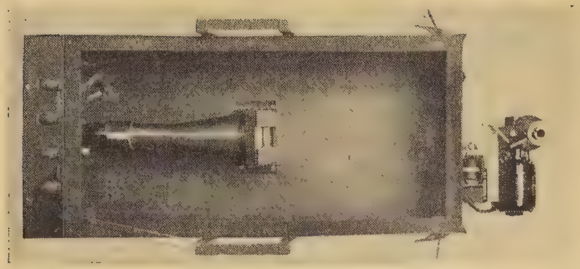

Figure 2. Top view of special cathode-ray oscillograph with cover and film drum removed, showing position of two tubes and lens

so that the traces appear on the tube screens as unidimensional Lissajous figures in line with each other, and time resolution is provided by the motion of the recording film.

(b). Film Drum

An $f2.0$ lens, which may be seen inside the tube compartment at the end next to the driving motor in Figure 2, focuses the cathode-ray traces on a moving film. The film, 3 $^{7}/_{8}$ by 30 $^{1}/_{2}$ inches is held by spring belts on the rotating drum shown in its holder in Figure 3. The drum holder is equipped with a manually operated shutter and a removable cover for loading. It is readily attached to the oscillograph case by spring clips as shown in Figure 1, and, when in place, the axis of the drum with its key fits inside a hollow slotted driving shaft which in turn is geared to a small electric motor. The drum is free to move inside the holder a distance of several inches in a vertical direction, so that different portions of the film may be exposed to give several records on a single film. Its vertical position is determined by the position of a supporting threaded collar which may be moved up and down on the outside threaded driving shaft. In operation the drum is automatically shifted a predetermined space after each exposure. This is accomplished by causing a solenoid operated lever to engage the supporting collar. The collar then moves downward as the shaft continues to rotate. When the lever is not engaged the collar rotates with the shaft and has no vertical motion. The distance the film is shifted is dependent on the time the lever and collar are engaged and this time is determined by the setting of a time-delay relay which controls the solenoid. A limit switch removes all power when the drum attempts to shift past the final position. All of these elements are visible in Figure 1.

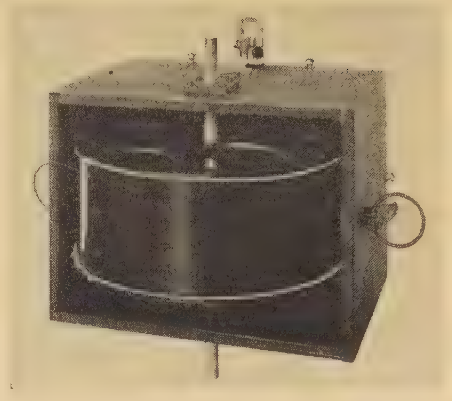

Figure 3. Film drum and holder with cover removed

Shutter visible at rear of holder

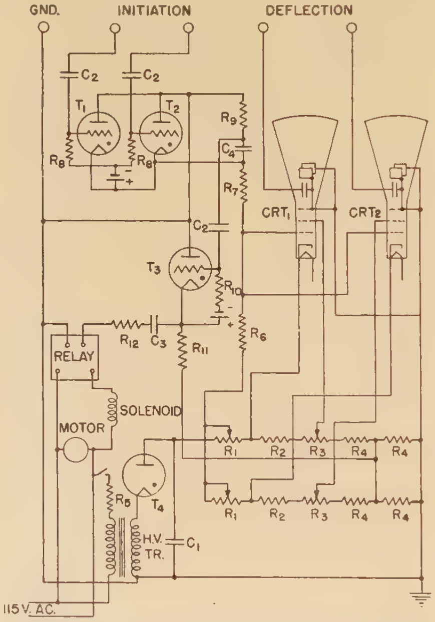

Figure 4. Schematic diagram of circuit for special cathode-ray oscillograph

(c). Circuit

The oscillograph circuit is shown schematically in Figure 4. Capacitance C_1 is kept charged to 1,300 volts to supply all d-c potentials. The cathode-ray tube excitation and control voltages are obtained from the voltage divider consisting of R_1, R_2, R_3, and R_4. The beam intensity is controlled by adjustment of R_1, and, when this is set just at cut-off, the circuit involving thyratrons T_1 and T_2 may be used to initiate the beam automatically for a predetermined length of time.

For such automatic initiation a positive pulse is applied to either initiation terminal. The double input permits tripping from a surge of either polarity by supplying the trip indication through a transformer or coil with a grounded midpoint. When the trip impulse is received at the grid or grids, either T_1 or T_2 fires and discharges C_4 through R_9. This causes current to flow through R_6 and R_7, thereby reducing the cathode-ray tube-control grid bias enough to establish the beam. When C_4 is discharged, T_1 and T_2 regain control since the current through R_7 is too small to maintain ionization. However, the cathode beam is maintained until C_4 is recharged and this time may be adjusted over wide limits by selection of proper values for R_6, R_7, and C_4.

When C_4 begins to discharge, a positive pulse is applied through C_2 to the grid of thyratron T_3. This fires T_3 and discharges C_3 through the time-delay relay circuit. After this indication the time-delay relay circuit begins its sequence and after a delay of several seconds energizes the film-shifting solenoid for the period of time set by the relay adjustment.

(d). Operation

The normal sequence of events in obtaining a record with the oscillograph is as follows:

1. With the loaded film drum in place and set for its highest position, the tubes are energized and the motor started.

2. The shutter on the film holder is opened manually, and the tripping impulse is applied to the initiation circuit. This produces two traces on the film showing whatever voltages were on the deflection plates during the time the beam was on.

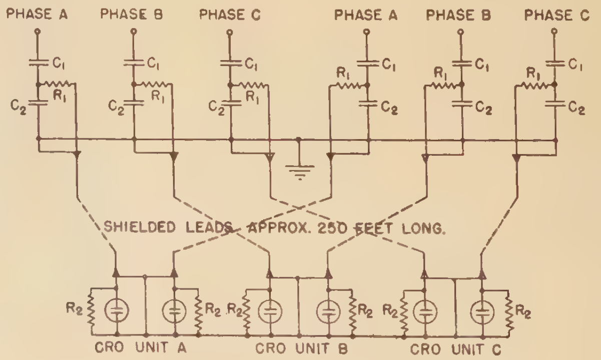

Figure 5. Voltage-divider and cathode-ray-oscillograph connections for measurements on 230-kv system at Boulder power plant

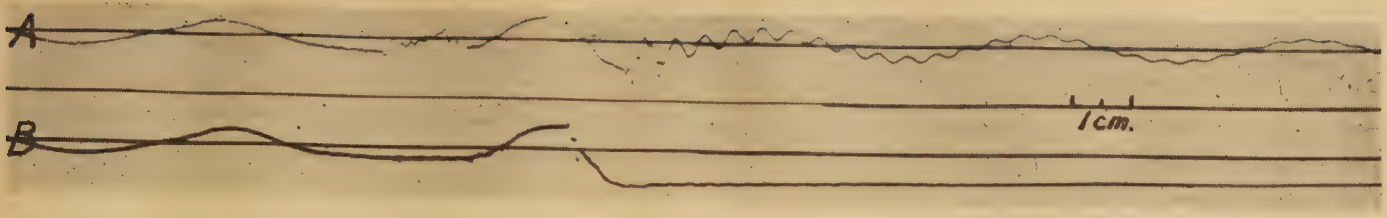

Figure 6. Sample cathode-ray oscillogram of line-dropping test on 230-kv system

A. Bus side of oil circuit breaker, voltage to ground, phase A
B. Line side of oil circuit breaker, voltage to ground, phase A
Time progresses from left to right
Scale: 1 centimeter = 4,400 microseconds

3. The film is automatically shifted to expose a fresh portion.

4. Steps 2 and 3 are repeated if desired.

5. The oscillograph power is removed and the film is ready for development.

Measurements on 230-Kv System

While the oscillograph was not originally designed for switching transients in particular, it was available and worked out very well when such an application presented itself.

When plans were made to conduct line-switching tests with a 230-kv oil circuit breaker on the system of the Southern California Edison Company Ltd. at Boulder power plant, the feasibility of taking cathode-ray oscillograms of the voltage transients was considered. It was known that the transient frequencies involved would not be very high because of the large capacitance of the connected transmission lines. In fact, the expected frequencies were not above the capability of a magnetic oscillograph. However, no potential transformers were available for the measurement of voltages which might reach crest values of several hundred kilovolts, and the capacitance potential devices normally used to measure steady-state voltages could not be expected to give true transient response with a magnetic oscillograph connected. A cathode-ray oscillograph, on the other hand, could readily be used with capacitance voltage dividers and, in addition, could be relied on to follow accurately any abrupt voltage changes such as would be associated with restriking.

On this basis it appeared that the cathode-ray oscillograph described above would be suitable, and plans were laid accordingly.

Since several of these two-tube units were available, it was decided to use three and measure voltages to ground for both line side and bus side of the breaker on each phase. Capacitance potential devices were also available in each of these positions. Such measurements involved the subtraction of two voltages to determine the voltage across a pole of the breaker but provided the desirable measurement of the actual voltages to ground to which the system insulation was subjected.

(a). Installation

The oscillographs were set up in a room under the switchyard where the breaker and capacitance potential devices were located, and the cathode-ray tubes were connected to their respective voltage dividers by shielded cables approximately 250 feet long. The connections were as shown in Figure 5, and it may be seen that the arrangement was such as to give the voltages on both sides of each breaker pole on a single film.

The voltage dividers were contrived by using the coupling capacitor units installed on the two outgoing transmission circuits, and these constitute the C_1 units in Figure 5. The C_2 units were made up of small radio-type capacitors. Two different values were used depending on the expected voltage. Ratios for the two conditions were 8,300 to 1 and 4,600 to 1 which provided for recording up to crest voltages of about 800 and 450 kv respectively.

During a test the regular potential network and carrier-current equipment were disconnected from the coupling capacitors, and connection was made to the C_2 units. Connection to the cables from the voltage dividers was through resistances, R_1, equal to the cable surge impedances. At the oscillograph end each tube was provided with a leakage resistance, R_2, to prevent accumulation of charge on the deflection plates from stray electrons of the cathode beam. All of the oscillograph grounds were connected to the cable shields, and actual grounding was done at only one place. Care was taken to minimize loops between voltage leads and ground returns.

(b). Operation

The oscillographs were initiated simultaneously by the application of a positive battery voltage. This was accomplished by a relay which was energized at the same time as the trip coil of the circuit breaker.

The film-shifting feature proved very convenient in that it was possible to record all voltages for each series of five tests without changing films. For this reason no delays in the testing schedule were chargeable to cathode-ray-oscillograph operation.

Voltage calibrations were obtained by recording steady-state voltages with the cathode-ray tubes connected to one set of voltage dividers while the line voltages were read in the usual way with the other set of potential devices in normal operation. This procedure was repeated with the roles reversed for the two sets of potential devices to complete the calibrations for all six tubes and dividers.

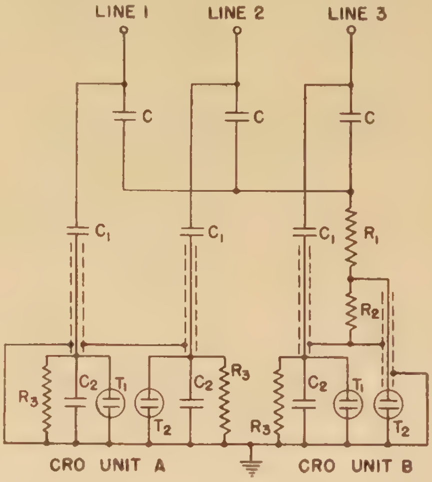

Figure 7. Voltage-divider and cathode-ray-oscillograph connections for measurements on the 15-kv system of a large industrial plant

A complete set of oscillograms was obtained for each of the 41 tests made at Boulder power plant. The results are analyzed elsewhere,[4] but a sample record is shown in Figure 6. For these tests a film speed of approximately 90 inches per second was used with a beam duration of about a half-second. This gave some overlapping of the record but was not objectionable. Zero lines were provided by tripping the beams on for an exposure with the deflection plates grounded.

It is interesting to note in passing that the entire equipment for these measurements, including shielded cable and dark-room supplies, had a shipping weight of less than 900 pounds.

Measurements on a 15-Kv System

Since the low-voltage two-tube oscillograph worked out so well on the Boulder power-plant application, it was the logical choice when cathode-ray oscillograms were required on some recent capacitor switching tests. These tests were on a 15-kv circuit at a large industrial plant. In this case line-to-ground voltages on the capacitor side of the breaker only were required, and two of the two-tube units were used. With one tube for each phase, the spare measuring element was connected to measure the voltage fluctuations of the capacitor neutral.

(a). INSTALLATION

The oscillographs in this case were set up in a temporary shack immediately adjacent to the circuit breaker and capacitor installations. The connections which are shown in Figure 7 were essentially the same as those used at Boulder power plant with one exception. In this case the distance from the high-voltage capacitors to the oscillographs was not over ten feet and so the C_2 capacitors were mounted at the oscillograph end of the shielded leads.

The high-voltage capacitors (C_1) in this case were not regularly installed equipment but were provided with the oscillograph equipment. They consisted of porcelain wall tiles with inside and outside surfaces coated with metal foil over a sufficient area to give a capacitance of 200 micromicrofarads. The C_2 capacitors were again of the radio type and the ratio used was 300 to 1, which permitted recording up to approximately 30 kv.

For the measurement of the neutral voltage a resistance divider was used. This consisted of a string of ten two-watt metalized resistors totaling 50,000 ohms (R_1) in series with a 500-ohm unit (R_2) across which the cathode-ray tube was connected to give a ratio of 100 to 1 and recording up to ten kilovolts.

Again a single ground connection was made, in this case at the oscillographs.

(b). OPERATION

Initiation of the oscillographs was accomplished in the same manner as at Boulder power plant except that the relay was energized simultaneously with the starting of the magnetic oscillographs. From one to five records were taken on each film depending on the needs of the testing schedule.

Since magnetic oscillograms of the voltages on the bus side of the breaker were taken, it was possible to obtain voltage calibrations for the capacitance dividers by comparing the magnetic oscillograms with the cathode-ray oscillograms at times when the breaker was closed and steady-state voltages were present. Calibration for the resistance divider was obtained from direct measurement of the resistances R_1 and R_2.

Oscillograms were obtained for 52 tests, including both opening and closing operations of the breaker. A film speed of 90 inches per second and beam duration of a half-second were used again and zero lines were provided as before. A sample record is shown in Figure 8.

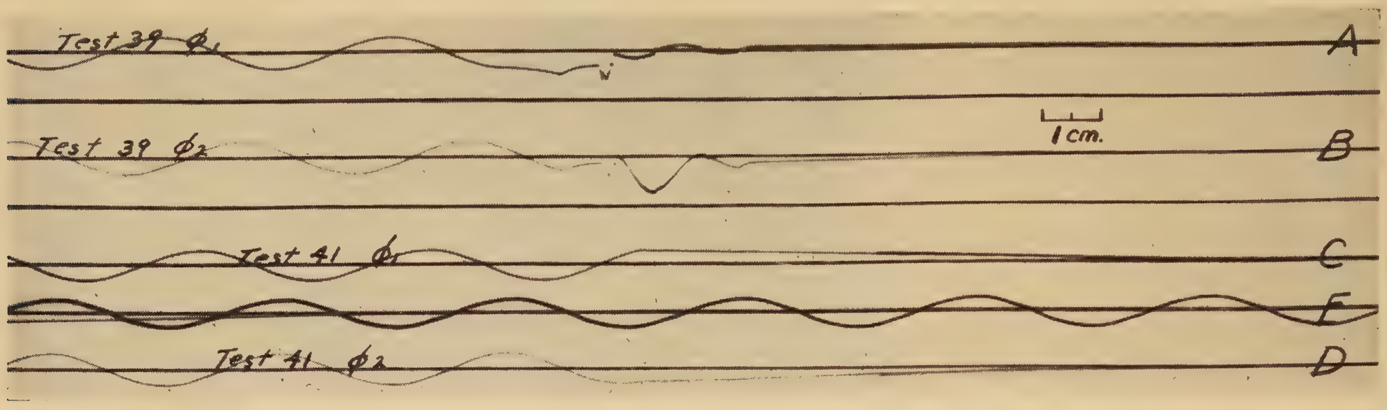

Figure 8. Sample cathode-ray oscillograms of capacitor switching tests on 15-kv system showing several tests on single film

A. Capacitor voltage to ground, phase 1, test 39
B. Capacitor voltage to ground, phase 2, test 39
C. Capacitor voltage to ground, phase 1, test 41
D. Capacitor voltage to ground, phase 2, test 41
E. Steady-state 60-cycle voltage on phase 1. Prior to test 42
Time progresses from left to right
Scale: 1 centimeter = 4,400 microseconds

Conclusion

The preceding paragraphs have given a description of a special cathode-ray oscillograph incorporating a number of new features. This oscillograph uses the inexpensive low-voltage tube found in thousands of small oscilloscopes used in practically every laboratory, radio service shop, and engineering classroom of the country. It, however, utilizes the features of moving film recording and automatic-beam control ordinarily found only in high-voltage equipments. In addition it uses two tubes to provide for the simultaneous recording of two phenomena.

It also has been shown how this oscillograph by actual performance in the field has proved itself well-suited to the measurement of long-time transients. It compares favorably with the high-voltage cathode-ray oscillograph if high recording speeds are not required, especially if cost and size are factors to be considered. It, of course, is not to be considered as a general substitute for the magnetic oscillograph since it does not have so many recording elements. Rather it is an additional measuring device which realizes with low cost and simplicity the peculiar advantages of the cathode-ray oscillograph, low energy drain and inertialess response, for the problem of long-time transient measurement.

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Factors Involved in the Selection of Railroad Motive Power

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Synopsis: The paper presents the position of the three types of motive power in railroad service and offers a brief historical background to show how each one arrived at its present status. A table lists advantages and disadvantages inherent in each type and includes supplementary information to explain the listings in the table. The subject is presented from the viewpoint of the user, and a method of determining more closely the place for each type of locomotive is suggested. An appendix lists all of the considerations which must be included in a complete cost study.

FOR 60 years the steam locomotive was without competition, as a form of motive power, on American railroads. Its first serious competitor, the electric locomotive, appeared in 1895 and during the following 40 years displaced steam in several specific applications. The reasons for this were that the electric locomotive possessed certain inherent advantages that made it both more desirable and economical for these applications. Electrification did not displace steam operation completely, because experience demonstrated its limitations. The Diesel-electric locomotive made its initial appearance in this country—in switching service—in 1925, but, as in the case of the electric locomotive, almost ten years went by before the industry accepted it as a competitor. From 1935 to date the Diesel-electric locomotive forged ahead more rapidly than either of its competitors—electric or steam—in any similar period. There were many reasons for this. Outstanding among these are that the Diesel-electric has most of the advantages of both electric and steam motive power without too many of its disadvantages, plus an appeal to the public that increased the revenues of the railroads when an increase was desperately needed. What, now, are the limitations of the Diesel-electric locomotive, and to what extent can either the electric or Diesel-electric be expected further to encroach upon the dominions of steam.

One hundred years of railroading have demonstrated that, in the final analysis, any controversy that exists as to which is the best type of motive power must be decided upon the basis of sound economic principles. Experience has also demonstrated that there is no universal formula by which the user can determine whether one or the other type should or should not be used, but there are certain fundamentals upon which agreement can be reached, through the application of which a final choice may be made. No application of these fundamentals can be made until the problem is clearly stated.

Historical Background

The steam locomotive came into being more than 100 years ago as a competitor to the horse and the canalboat. From that point it grew in size and power, and the railroad grew with it, including the enginehouse, the shop, water and fuel services, and all facilities required to maintain and operate steam power. Most railroad operation is designed around the characteristics and requirements of the steam locomotive.

The electric locomotive was first applied to trunk-line railroad service in 1895, when it was used to eliminate a smoke hazard by hauling trains with their steam locomotives through the Baltimore and Ohio tunnel at Baltimore Md. From this beginning its use was extended to the operation of congested city terminals where smoke elimination was important, to operation of trains on heavy grades (including tunnels), to suburban passenger service requiring short headways and rapid acceleration where smoke elimination was also a factor, and to heavy-traffic general service lines where the added cost of the power distribution system could be justified by savings in operating cost and improved service.

The Diesel-electric locomotive was developed from the rail car, the latter having been used for several years to take care of diminishing traffic on branch lines. The first locomotive of this type to be used on American railroads in 1924 was in fact a rail car equipped with a heavy-duty Diesel engine and electric transmission. From this beginning the application of Diesel-electric locomotives to switching service has been extended until a railroad purchase of a new steam switcher is exceptional.

Road application of Diesel engines began with lightweight passenger trains in which the first car contained baggage and mail compartments, in addition to the space occupied by the power plant. These trains were increased in size, and the motive power grew from one to two, and finally to a three-unit Diesel-electric locomotive. Four-unit locomotives are used in freight service, and some Diesel-electric locomotives are used interchangeably in freight and passenger service.

A common error made by transportation engineers, for reasons best known to themselves, in dealing with the selection of motive power has been to approach the problem primarily on the basis of substituting one type of motive power for another, whereas actually the ultimate solution may be an economically sound use of combinations of different types—each in the place where and to the extent to which it is best suited.

Table 1. Analysis of Motive-Power Characteristics

	Electric	Steam	Diesel-Electric
1. Operating Cost			
(*a*). Fuel	*D*	*D*	*A*
(*b*). Water	*A*	*D*	*A*
(*c*). Lubricants	*A*	*A*	*D*
(*d*). Enginehouse expense	*A*	*D*	*A*
(*e*). Supplies (of no great importance)			
2. Fixed Charges			
(*a*). First cost	*D*	*A*	*D*
(*b*). Depreciation	*A*	*A*	*D*
(*c*). Interest, taxes and insurance	*D*	*A*	*D*
3. Operating Characteristics			
(*a*). High acceleration	*A*	*D*	*A*
(*b*). Overload capacity	*A*	*D*	*D*
(*c*). Availability	*A*	*D*	*A*
(*d*). Flexibility	*A*	*D*	*A*
(*e*). Use of factor	*D*	*D*	*A*
(*f*). Freedom from complete failure	*D*	*D*	*A*
4. Special Features			
(*a*). Smoke elimination	*A*	*D*	*A*
(*b*). Damage to roadbed	*A*	*D*	*A*
(*c*). Merchandising appeal	*A*	*D*	*A*
(*d*). Dynamic braking	*A*	*D*	*A*
5. Possibility of Eliminating Facilities			
(*a*). Operating	*A*	*D*	*A*
(*b*). Maintenance	*A*	*D*	*A*

A—Advantage. *D*—Disadvantage.

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The first requisite is to state the problem; in simple terms it is how to decide whether or not to use steam, Diesel-electric, or electric motive power, or combinations thereof, for performing the required functions of a railroad. A study of the problem may, for example, indicate that, to do a specific job, a railroad should use Diesel-electric switchers, Diesel-electric road passenger locomotives and steam freight locomotives, or possibly Diesel-electric switchers and electric road passenger and freight power. It is quite possible that, to the man with an open mind, the very effort to prove the case for one type of motive power as against another may demonstrate the logic of utilizing combinations.

The second requisite is the necessity of collecting a variety of data relating to the specific application under consideration. The nature of those data may be seen by reference to Appendix A.

Once the basic data have been collected and studied, an equal familiarity with the characteristics of the three types of motive power should be acquired. With that end in view Table I has been prepared. For those interested in the reasons why any given characteristic is considered an advantage or a disadvantage, it may be of value.

Fuel

When the cost of coal delivered to the tender is four dollars a ton, the cost of coal is about the same as the cost of power required for an electrified line. This relation will vary considerably, depending upon the power contract, the amount of transmission and distribution losses, and so forth. But experience indicates that if the cost of coal is less than this, no savings in fuel cost can be made by electrifying a steam-operated railroad. For class I railroads the average cost of coal delivered to the railroad is $2.68. To this must be added the cost of distributing it to the coaling plants and loading it on the tender. The cost of handling coal from the car to the locomotive (including fixed charges) is about 15 cents per ton. Obviously, the railroads which are remote from sources of supply pay a greater hauling charge than those with short hauls from the mines.

Fuel costs for steam and Diesel-electric locomotives can be compared best from performance records. One railroad using both types for freight service reports a fuel cost of 33 cents per mile for coal-burning steam locomotives and 28 cents per mile for Diesel-electric locomotives in the same service. These figures favor the steam locomotive somewhat, since in this instance the Diesel-electric locomotives are operated in high-speed service, whereas the steam locomotives are used at lower rates of speed.

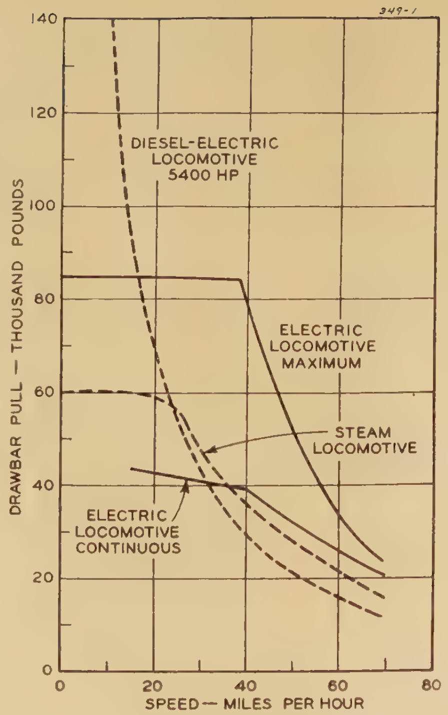

Figure 1. Comparative drawbar-pull–speed curves of electric, steam, and Diesel-electric locomotives on one per cent grade

Type Locomotive	Weight on Drivers	'Total Weight
Electric	360,000	460,000
Steam	281,440	1,050,010 (incl. tender)
Diesel-electric	856.000	856,000

In passenger service one road reports fuel costs of 17 cents and 10 cents for steam and Diesel-electric locomotives, respectively, a saving of 41.1 per cent. In two other cases the savings reported are, respectively, 16.3 and 39.8 per cent.

It is in switching service that the Diesel-electric locomotive makes the greatest saving of fuel. There are three reasons for this. One is the inherently high thermal efficiency of the Diesel engine. Another is the relatively high standby losses of the steam locomotive, and the third is the electric transmission which permits the Diesel engine to operate near its full-load rating at all locomotive speeds. Hourly fuel costs taken from a recent report are shown in Table II.

Water

The average water cost for a steam locomotive is approximately ten per cent of the fuel cost. On Diesel-electric and straight-electric locomotives, water is a relatively small item, even in passenger service which requires oil-fired boilers on the locomotive for train heating and for the steam-ejector type of air conditioning. The amount of water used for this purpose is only about one tenth of the total water used for all purposes by a steam locomotive in the same service. In freight and switching service, the electric locomotive uses no water, and the amount needed by the Diesel-electric locomotive is practically negligible.

Lubricants

An empirical method of approximating lubricating costs for Diesel-electric locomotives is to assume that the consumption of lubricating oil is from $1^1/_2$ to 3 per cent of the fuel oil, whereas the cost per gallon is ten times as much. Thus the cost of Diesel-electric lubricating oil is from 15 to 30 per cent as much as the fuel cost. Steam locomotives doing the same work will consume lubricants costing about half as much as those used by the Diesel-electric locomotive, and accumulated experience shows that the cost of lubricating electric locomotives is much less than that for steam.

Enginehouse Expense

Enginehouse expense is a disadvantage of the steam locomotive. In road service it may be from 1.6 to 4 times as much for the steam as for the Diesel-electric locomotive, while in switching it may be from 5 to 12 times as much. Thus on roads using both types of power we find such costs, as are given in Table III.

Electric-locomotive enginehouse expense is less than that of the Diesel-electric, since it is essentially a Diesel-electric without the Diesel engine. The high steam costs arise from the fact that the steam locomotive requires periodic attention for the cleaning of flues, boilers, grates, and so forth, with the resulting need for resteaming and attention to fires.

Locomotive supplies are an item included in all locomotive cost accounting, but they are a small one, and there is so little difference for each type that this is not a controlling factor.

Smoke Elimination

Electric locomotives are completely effective in eliminating smoke. They have been responsible for the transforming of smoking yards into first-class residential districts and for the safe operation of trains through long tunnels. Diesel-electric locomotives produce little smoke, but their exhaust gas and the fuel they carry have precluded their use in some terminals.

Damage to Roadbed

Electric and Diesel-electric locomotives which have no unbalanced weights in their wheels do not produce what is known as dynamic augment. This adds to the problem of steam-locomotive design for high speeds and in the case of slipping wheels may result in track stresses sufficient to kink the rails. The smaller wheel diameters and lower center of gravity used with Diesel-electric locomotives increase shearing stresses at the head of the rail, but this is largely offset by lower axle loads. It is generally accepted that the electric and Diesel-electric cause less damage to roadbed than steam locomotives, but much work is being done to improve the characteristics of the latter at high speeds.

Merchandising Appeal

Diesel-electric-powered streamlined trains were introduced in 1933, and the great increase in passenger traffic between 1933 and 1941 is good evidence that people like them and will desert the highway for them. A part of the credit for this shift must be given to fare adjustments and a part to air conditioning, but undoubtedly the Diesel-electric locomotive deserves its share.

At a meeting of the American Association of Passenger Traffic officers in November 1941, an attempt was made to evaluate the influence of passenger service on freight traffic. A poll of railroad traffic officers showed that none think it of no importance, and 84 per cent of those questioned feel that the influence of passenger service is either a major or an important factor. A somewhat different attitude is shown by traffic managers who buy transportation. Almost half of a number of these men who were questioned said that the great bulk of freight traffic is routed without consideration for passenger service. Even though the latter group is most nearly correct, it is obvious that passenger service is an important influence.

Dynamic and Regenerative Braking

Dynamic braking has been developed for the Diesel-electric locomotive. This consists of using the traction motors as generators and dissipating the energy generated in resistors. The brakes are designed to hold, at a definite speed on a descending grade, the same train weight that the locomotive can haul up the same grade at the same speed. In actual operation, on descending grades, train weight may be heavier, and speeds maintained are higher than on the same grade ascending, so that for controlling such trains some assistance for the dynamic brake is required from the train air brakes. In trials on various grades, the amount of train air braking required was reduced to approximately one fourth of that required by conventional braking with train air brakes. This represents an important reduction in brake shoe and wheel wear and reduces damage to wheels by overheating. It is also a safety device.

The electric locomotive is capable of employing regenerative braking—of returning the power generated in the motors to the power system. It is definitely superior to dynamic braking for improving train operation, since it allows closer speed regulation on the down grades, and in actual practice on heavy grade lines it makes an appreciable saving of the electric energy required by the railroad.

Fixed Charges

In the matter of fixed charges against operation, we are concerned with two major factors: first cost and service life. Originally, the Diesel-electric locomotive was at a distinct disadvantage with respect to steam because of high initial cost. However, progress in design and production methods, as well as an increased volume of production, has reduced the initial cost of Diesel-electric locomotives to approximately $87.50 per engine horsepower as compared with approximately $35.00 per cylinder horsepower for steam.

The cost of an electric road locomotive is about $65.00 per continuous horsepower at the rail. To this must be added the cost of the power-distribution and contact system, but no close estimate of its cost can be made since, roughly speaking, the same system that serves ten locomotives can also serve a hundred. If a substitution of Diesel-electric or electric locomotives for steam is being considered, some credit should be given to the replacing units for fuel-, water-, and ash-handling systems and such enginehouse and shop facilities as are not required by electric or Diesel-electric power. In any plan for improvement some real thought should be given to the elimination of terminals made possible by improved locomotives and better operation.

To become involved here in any discussion of the depreciation question would serve no good purpose. Experience with steam and electric locomotives has demonstrated that a depreciation rate somewhere in the neighborhood of 4.5 per cent well serves the purposes of the railroad industry's present methods of accounting. In the matter of Diesel-electric locomotives, the question of depreciation has been approached in a somewhat different manner than has been the case with steam. By this is meant that recognition has been taken of the fact that a Diesel-electric locomotive is, for example, 40 per cent Diesel engine, 40 per cent electric equipment, and 20 per cent mechanical equipment. Because of this fact, three depreciation rates might be desirable: one to the mechanical equipment, one to the electric equipment, and one to the Diesel engine.

Years of experience with both mechanical and electric equipment, such as is now used on Diesel-electric locomotives, have simplified the problem of establishing depreciation rates. The unknown factor has been that of the service life of the Diesel engine. It is quite probable that time will demonstrate that the economic service life of railway-type Diesel engines may be longer than was anticipated in the early days of their service on the railroads. To summarize this matter of depreciation without offering any conclusion, it may be said that individual roads have established depreciation rates for Diesel-electric locomotives all the way from four to ten per cent annually.

Operating Characteristics

High acceleration is an inherent characteristic of both Diesel-electric and electric locomotives and a characteristic which has been lacking in steam locomotives, particularly of older designs.

Overload Capacity

The electric is the only one of the three types of motive power which has real overload capacity. Steam locomotive horsepower depends upon mean effective piston pressure and speed, and horsepower outputs near the maximum are obtainable over only a short portion of the speed range.

The maximum horsepower output of a Diesel-electric locomotive is dependent upon the capacity of the Diesel engines.

Table II. Hourly Fuel Costs

	Steam	Diesel-Electric	Saving
Railway *A*—600-hp Diesels	$0.801	$0.224	$0.577
Railway *B*—600- and 1,000-hp Diesels	1.120	0.370	0.750
Railway *C*—1,000-hp Diesels	1.0636	0.3267	0.7369

It cannot be increased above this amount, but the electric transmission permits the use of this horsepower over a relatively large part of the speed range.

The relation of continuous tractive force to maximum tractive force of an electric locomotive is shown in Figure 1. Because the electric locomotive can draw from the power-supply lines up to the heating limits of the motors, it can for short periods deliver horsepower outputs which are almost twice the continuous rating. Thus a locomotive having a continuous rating of 4,860 horsepower at 65 miles per hour may be capable of outputs at the driving wheels up to 9,000 horsepower when the service conditions demand. In so far as performance is concerned, it may be looked upon as the ideal locomotive. In actual practice, an electric locomotive having 72 per cent of its weight on drivers can exert enough tractive force to slip its drivers at any speed.

To obtain this overload capacity, operation must be so arranged that the motor temperatures are well below maximum permissible values before the extra horsepower is called for. One overload must not be superimposed on another. Within the limits of its horsepower capacity the Diesel-electric locomotive is similarly limited.

Availability

When a steam locomotive arrives at its terminal, it requires certain servicing and repair operations. A study recently made by one railroad indicates that steam freight locomotives are in service approximately 12 hours out of a 24-hour period, that they spend eight hours at terminals undergoing servicing and repairs and the other four hours waiting for something to do. This corresponds to an availability factor of $66^{2}/_{3}$ per cent. As compared with this figure, the Diesel-electric locomotive will have an availability factor of possibly 90 per cent and the electric locomotive perhaps 95 per cent. Availability of any type of motive power can be improved by the intelligent use of modern terminal facilities.

Table III. Enginehouse Expense

	Steam	Diesel-Electric
Freight service (per mile)	$0.04	$0.01
Passenger service (per mile)	0.02	0.012
Switching 1 (per hour)	0.393	0.038
Switching 2 (per hour)	0.2279	0.0427
Switching 3 (per hour)	0.47	0.04

Flexibility

The flexibility of Diesel-electric locomotives is an "advantage characteristic" brought about by the fact that it is built in the form of "packaged power": for example, in 1,000- or 2,000-horsepower units, from one to four of which may be used to provide the power required for a variety of operating conditions. When, for example, a 5,000-horsepower steam locomotive is used on a "1,000-horsepower job," the loss to the operating company entailed by the resultant low load factor is obvious.

What has been offered in this paper may be considered as a pattern or design for procedure. It is suggested that it now be implemented by setting up a set of specific conditions on a railroad or a section of a railroad. This specific case could then be placed in the hands of three groups of recognized transportation engineers experienced in the application of steam, electric, and Diesel-electric motive power. Each group could then proceed to determine the extent that each type should be used for this specific case. When put together, the three reports would indicate to what extent definite values can be given to the many factors involved.

Appendix A

Data Required for the Selection of Proper Motive Power for a Specific Service

1. No rule-of-thumb method for selection of type.
2. Data required for selection of type:
    - Density of traffic.
    - Permissible hours of operation per year.
    - First cost of each type plus attendant facilities.
    - Maximum starting tractive force.
    - Maximum running tractive force and speed.
    - Limiting track and bridge conditions.
    - Clearance diagram.
    - Data on all track curves.
    - Maximum operating speed.
    - Maximum tonnage of train and number of cars (empties and loads).
    - Complete profile and track alignment.
    - All speed restrictions.
    - Operating schedule with normal train.
    - Operating schedule with maximum train.
    - All intermediate stops and stop time.
    - Helper zones and conditions.
    - Altitude and climatic conditions.
3. Method of determining physical characteristics and capacity:
    - Determine weight required on drivers.
    - Pick wheel arrangement to meet track curve and speed requirements.
    - Estimate power requirements, and justify by the necessary speed-time calculations.
    - See that this power may be built into the clearance limitations and with above wheel arrangement.
4. Data required to compute operating costs:
    - Wage agreements.
    - Number of men in crew.
    - Coal or steam oil-fuel cost (including all transportation and handling).
    - Steam fuel consumption.
    - Diesel fuel cost (including all transportation and handling).
    - Diesel fuel consumption.
    - Electric-power cost (rates and demand charges).
    - Electric-power consumption (including losses from source to locomotive).
    - Combined power-consumption chart (based on train sheet).
    - Lubrication cost for each type of locomotive.
    - Water cost and consumption for each type.
    - Cost of other supplies.
    - Enginehouse expense for each type (actual, not assumed).
    - Repair costs averaged for 20 years for each type.
    - Operating cost of distribution equipment (electrification only).
    - Maintenance cost of distribution system (electrification only).
    - Operating miles or hours per year for each type of power in each class of service.
5. Data required to compute fixed charges:
    - First cost of each unit of motive power.
    - Plotted train sheets to determine number of motive power units including spares.
    - First cost of all necessary attendant facilities.
    - Interest rate on borrowed money.
    - Depreciation rate for each locomotive and facility.
    - Tax rate.
6. Total cost to own and operate each type:
    - Compute cost of operation per mile or hour.
    - Multiply by total hours of operation per year (including relief).
    - Add all fixed charges.

There is no rule-of-thumb method by which the proper type of motive power (steam, electric locomotive or multiple-unit car, Diesel-electric locomotive or Diesel-electric railcar) may be selected for a given service. Whereas it is true that experience has proved that one or another type of power is the most economical for a specific operating condition (a Diesel switcher is economical where it can be operated 24 hours per day), general classes of service cannot be so segregated. The density of the traffic, the permissible hours of operation per year, the first cost of the motive power and facilities, the relative operating costs of each, and other pertinent conditions must all be studied. The answer will be found in the summation of costs for owning and operating each type.

The study which must be made in order to arrive at an intelligent selection of the most suitable and economical locomotive or car involves three general steps:

1. Determination of the physical characteristics and horsepower capacity of the vehicle.
2. Compilation of the comparative yearly operating costs of each type under consideration.
3. Addition of the yearly fixed charges of the motive power and its appurtenances (which include the power distribution system in the case of electrification).

Selection of Locomotive Characteristics

The selection of the proper locomotive weight and wheel arrangement and a determination of the power which should be built into the locomotive must be the result of careful study of all of the operating requirements to be met. Among these are:

Maximum starting requirements.
Maximum running tractive force at a known speed.

Limiting track or bridge conditions.
Clearance diagram.
Sharpest curves which must be negotiated.
Maximum speeds of operation.
Maximum tonnage to be hauled.
Complete profile and track alignment.
All speed restrictions.
Operating schedule with normal train.
Operating schedule with maximum train
All intermediate stop time.
Grades where helpers may be used.
Altitude and climatic conditions.

The weight per driving axle is limited by track and bridge strengths, the minimum weight on all drivers is fixed by the maximum tractive force required (starting or running), the permissible rigid wheel base is determined by the sharpest curves to be negotiated, the use of guiding axles or trucks is a function of the maximum speeds, and the power which must be applied is determined by the train weights, profile, speed restrictions, and necessary schedules to be met. The steam or electric locomotive selected may be one which has been built previously or a modification thereof, while the Diesel-electric is usually the nearest commercial size (in production) which fulfills the conditions.

Operating Costs

With the operating conditions known, and a locomotive of each type selected (or possibly a railcar or multiple-unit cars), the next step is to determine the operating yearly costs for each system. There are seven general cost items which form the basis for compiling these figures. In addition, electrification carries two other charges—operation of substations and maintenance of distribution system. These charges, then, are:

Wages—enginemen and train crew
Fuel (steam or Diesel-electric) or power cost (electric) at the locomotive.
Lubrication.
Water (only for train heating boilers in Diesel-electric or electric locomotives).
Other supplies.
Enginehouse expense.
Repairs—running and class.
Operation of distribution equipment (electrification only).
Maintenance of distribution system (electrification only).

Since wage rates, fuel consumptions, and other cost items may vary with different train consists, all such figures should be compiled on the basis of yearly aggregates, taking into consideration the hours or miles (and costs) operated by relief power of another type.

Wage rates are subject to agreement between the railroad brotherhoods and the railroad, and wage-rate books are normally available. Enginemen are usually paid on the basis of weight on drivers, so that excess weight should be avoided.

The cost of coal for the steam locomotive should be the base rate (cost at the mine plus the freight charges to the lines of the railroad under study), plus a charge for hauling cost on its own line. To this must be added the handling cost per ton, which includes the operating costs on the coaling station. The fuel consumption rate and therefore the cost per mile or per hour must be determined from previous steam records for each train consist.

The cost of fuel for a Diesel-electric locomotive should be the cost in tank-car lots plus the two freight charges and the handling already enumerated. Approximately 260 gallons of Diesel fuel weigh a ton, so that the cost of transporting on company lines is small. The fuel consumption and therefore the cost per mile or per hour for each train consist may be determined by a consideration of the profile, Diesel-engine fuel rates, and engine loading for the complete service cycle, including engine idling.

The cost of electric energy must be obtained from a known power cost (rate plus demand charges), starting with the locomotive power consumption and adding transformation losses (transformers, rectifiers, rotary converters, motor generators, power-factor correction, and so forth, as used in the system considered), and distribution losses. The aggregate power consumption and the peak demands are determined by a close study of the power required by the individual trains over the profile and the train sheet from which a combined load curve is made.

Lubrication costs for each type of motive power are fairly well known. Lubrication costs of the Diesel-electric, because they include that of the engine, are usually much higher than for either of the other types of locomotives.

Water costs for steam locomotives are relatively high, as the other two types use water for train heat only.

Other supplies are a minor item but are usually higher for steam than for either of the other two motive-power units, although for multiple-cab Diesel-electric locomotives this cost may exceed that of steam.

Enginehouse expense is somewhat difficult to obtain. A steam locomotive requires considerable handling and care, including ash handling, boiler washing, and so forth. The Diesel-electric locomotive requires less attention than the steam locomotive, but more than is necessary for the electric locomotive. This item of cost is usually recorded as cost per engine handled (per engine turned).

For Diesel-electric power there is a tendency to assume that the enginehouse expense is low and arbitrarily to include a low figure in the cost comparison. This is sometimes an erroneous method, however, for unless the use of the Diesel-electric locomotive actually results in a reduction of enginehouse personnel, there is no real saving to the railroad. This is often the case where one or two Diesel-electric locomotives operate out of an enginehouse handling many steam locomotives. The best way to figure the Diesel-electric enginehouse expense is to compute the actual enginehouse-expense saving (not to be confused with savings in personnel as a result of lower repair labor required), and subtract this from the normal steam expense to give the Diesel-electric expense figure.

In an electrification it is frequently possible to eliminate all steam power from the enginehouses, and this will effect very substantial savings in enginehouse costs.

Repair costs for steam power must be prepared from averages of similar operations over a long period of years corrected to the present-day value of the dollar. The reason for this is that certain costly repairs (classes I, II, and III) occur more or less infrequently and yet must be considered in the averages for the life of the locomotive. Similarly, Diesel-electric and electric motive-power-repair expense figures must have a contingent amount included over and above normal current repair costs to cover reconditioning of long-life items. It may be pointed out that while the repair costs of the running gear and electric equipment are nearly comparable for Diesel-electric and electric locomotives, the repair cost of the Diesel-electric will always be much higher than for the electric unit, because of the engine-repair costs, and also because it may be necessary to use a multiplicity of Diesel-electric cabs to do the work which may be performed by a single electric cab.

Locomotive Utilization

With all of the costs tabulated, the operating cost per year for each method of propulsion may be determined by combining these with the cost of the necessary relief-locomotive costs where the relief locomotive differs in type. In the case of steam or electric operation, it may be assumed that sufficient reserve motive power is available to fulfill the scheduled service with that type of power, but where relatively few Diesel-electric motive power units are available, the total miles or hours of Diesel-electric operation are lower than the yearly requisite by the amount of relief necessary. The total hours and cost must then be filled out by steam-locomotive relief time and cost.

In the case of switching locomotive power, it is often found that the Diesel-electric will cut the number of hours of service required to do a given amount of work. This is particularly true in congested yard service, where a Diesel-electric locomotive will switch from 20 to 33 per cent more cars per hour. In isolated service, however, a one-to-one ratio usually holds, except that the Diesel-electric locomotive will frequently reduce or eliminate the overtime hours which are necessary with steam. To determine the actual ratio of hours (straight and overtime), it is necessary to analyze all switching-locomotive assignments. In preparing a switching comparison of operating costs, it is usually desirable to segregate the cost tabulation into those services where the operation is continuous, where locomotives are assigned 16 hours per day, and where assignments are for one trick per day only. In the last case, an economic justification of Diesel-electric locomotives depends upon a very low Diesel-electric first cost as compared to steam.

Fixed Charges

To figure the total cost of owning and operating any type of motive power, it is necessary to include those annual charges which are applicable. These fixed charges, independent of the intensity of utilization, are:

Interest on the investment.
Depreciation charge.
Taxes.
Insurance.

Since it is necessary for railroads to borrow money to purchase new motive power, the annual interest charge should be figured on the purchase price plus the first years' interest charge, which is usually paid in advance.

The depreciation rate is usually higher for Diesel-electric locomotives than for either of the other types of power. When added experience with this type of motive power has been obtained, it may be found proper to establish a rate of depreciation comparable to that used for other types of power.

Taxes and insurance vary for different localities and must be determined on the same investment as is used for calculating the depreciation charge.

Total Yearly Cost of Each System

By totaling the fixed charges for any type of power and adding to its yearly operating cost, the annual cost of owning and operating each type is obtained. Since the selection of a system of motive power is usually based on financial considerations, the results of the tabulations should have considerable weight in determining the type of locomotive which should be purchased.

Comparative Figures Must Be Fair

In order to be of value in analyzing the relative merits of the different types of power, all figures and calculations must be fair. If the approach to such an analysis is biased by a predilection for any one of the types, there are many ways in which a preference may be shown. The results of such a tabulation, however, are valueless.

Field for Electrification

The traffic conditions which must exist in order to justify electrification are fairly well known. Unless the traffic density is high, or special conditions exist to warrant the cost of erecting a power distribution system, consideration of electrification is seldom warranted. It must be understood, however, that, where concentration of locomotive power is essential, no other type of propulsion as yet devised can compete with the electric locomotive.

Field for Diesel-Electric Motive Power

Diesel-electric motive power is extremely flexible in its range of operation and has fairly low operating costs. Where it may be used intensively, so that the fixed charges per mile or hour are low, the total cost of owning and operating units of this type is in its favor. For the large bulk of the locomotive operations in the United States, however, freight locomotives average but 102 miles per day and passenger locomotives but 166. Until the first cost of Diesel-electric power is not more than 50 per cent greater than that for satisfactory steam power, the savings which may be made by Diesel-electric road power will not justify the *replacement* of steam power in the average service. However, as the existing steam power must be retired because of age, the Diesel-electric savings need only justify the *additional* price necessary to acquire the Diesel-electric instead of new steam power.

Diesel-electric locomotives may be used more intensively than steam power. This results, in some instances, in replacing several steam locomotives with one Diesel-electric locomotive of the same train-hauling capacity (two or three to one).

Fundamental Basis for Motive Power Study

It should be emphasized that all comparative studies should start with a consideration of the characteristics of the railroad (profile, alignment, speed restrictions, and so forth) and of the maximum size of train to be handled. Where existing locomotives are handling a service, it is frequently possible to apply larger Diesel-electric or electric locomotives and show excellent economies. On the other hand, new steam power of the larger hauling capacity would also show improved economies. The exception to this, of course, is where the electric locomotive will afford a power concentration not available in either of the other types and thus effect savings or improvement in service not obtainable with other than electric locomotives.

The Power-Recovery System of Testing Aircraft Engines

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FOR its part in the development of the power-recovery system of testing aircraft engines the electrical industry may properly take pride. It has aided the war effort by helping to give to the aircraft-engine industry a testing technique that contains advantages not previously available. In this system of testing worth-while energy can be saved—more than half that required to operate an aircraft-engine manufacturing plant—and power demand can be reduced to the extent that power-demand costs and the capacities of power-supplying apparatus are materially reduced, engines can be tested with the ease and the quietness typical of electric machinery, testing can be carried on in buildings of simplified design, accurate electric load readings can be obtained to check and substantiate readings taken by conventional engine-testing methods, and, as reported by an aircraft engine builder, testing may be accomplished more quickly than with other systems. All of this can be done too on a basis that is economically sound.

What is the power-recovery system of testing aircraft engines? It is the purpose of this paper in its entirety to answer that question. How was this system of testing started, and what were the reasons for developing it? The answer to this latter question can be given in a few paragraphs.

The development of the power-recovery system of testing aircraft engines started in the fall of 1937 as a result of an inquiry from Pratt and Whitney Aircraft, division of United Aircraft Corporation, East Hartford, Conn.

In substance, Pratt and Whitney Aircraft asked: Can a system of testing be devised which will be economically efficient and which will make the loading of large engines easy and simple, and which will, at the same time, conform to the requirements of aircraft-engine testing?

The following, again in substance, is an explanation of why such an inquiry was made:

Extensive engine testing under loaded engine conditions has always been a necessary part of aircraft-engine *design and development* and a necessary part of regular everyday aircraft-engine *manufacture or production*. Such testing is necessary during development, because new designs, either of complete engines or of engine parts, must be verified by test. Such testing is necessary during manufacture, because it is by test only that each engine coming through production can be proved satisfactory and acceptable. The former class of testing, generally referred to as *laboratory or development* testing, is only of incidental interest in the present purpose. This development testing is generally accomplished by means of some form of cradled dynamometer or by means of such a dynamometer and a test stand on which the engine can be mounted and loaded by means of a propeller. The main issue and the real point of discussion is the latter form of testing, testing during manufacture, or what is commonly referred to as *production testing*.

Associated with production testing of aircraft engines to such an intimate degree that it becomes an inherent part of this testing is the breaking in of the engine. When an aircraft engine is placed in an aircraft, it must be ready to deliver its rated output. It must be ready to take off and fly.

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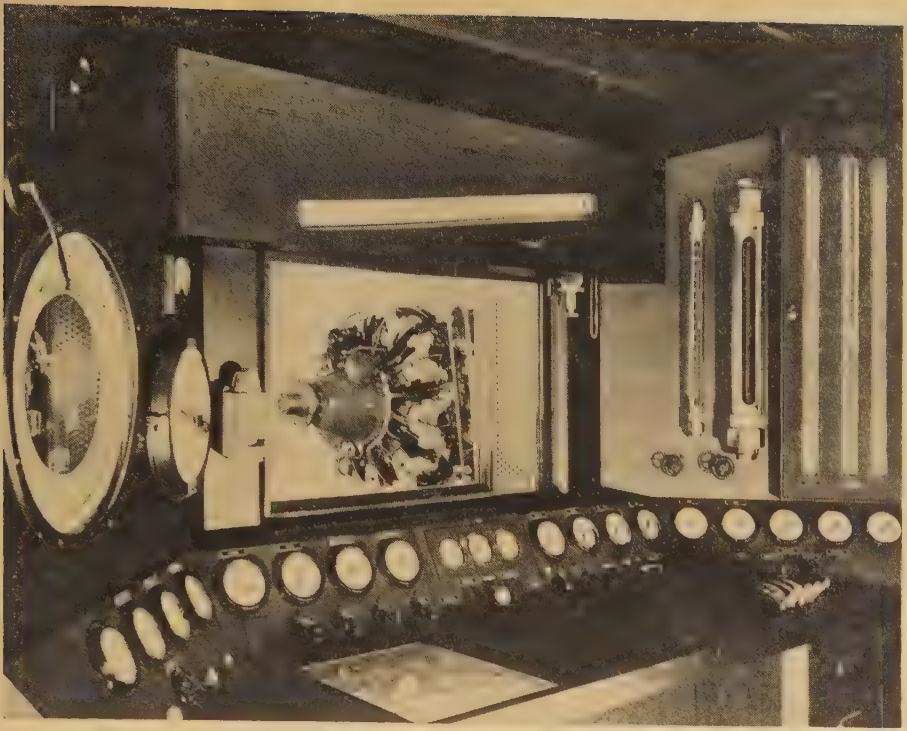


Figure 1. Aircraft engine in engine test room ready for moving in place for connection to power-recovery test stand

View is through the test operator's observation window. Test operator's control bench is in the foreground

Thus, for production testing of aircraft engines the aircraft-engine industry has always used testing techniques that involve several relatively lengthy loaded engine operating periods during which an engine is simultaneously proved in and broken in.

The situation in 1937 was that the only schemes used for production loading of aircraft engines were those involving propellers, water brakes, electric brakes, and similar devices, all of which involve disposal of engine power by complete wastage. The fuel consumed accomplished no useful purpose other than to permit testing the engine. Furthermore, engines were growing in size to the extent that testing schemes involving the disposition of power by wastage were beginning to present difficult problems.

The Pratt and Whitney Aircraft inquiry, therefore, led to the finding of a better and easier way to test aircraft engines and one in which the greater part of the power generated by the engine was recovered.

First Power-Recovery Testing Installation

The first solution was a power-recovery system employing a synchronous machine with a slip coupling.

Figure 2 shows schematically the arrangement of this power-recovery stand. This consists of a synchronous machine, which acts as either a motor or generator, and a hydraulic slip coupling interposed between the engine and the synchronous machine. Two such equipments were placed in operation at Pratt and Whitney Aircraft, East Hartford, Conn., in the spring of 1940. Figure 3 shows the electric part of a similar equipment subsequently supplied to another engine builder. The hydraulic coupling, to be added to the electric unit, will be mounted on the main base extension shown.

Operation of Slip-Coupling Synchronous-Machine Test Stand

It is an obvious truth that the only generally universal way to dispose usefully of the aircraft-engine output is to convert that output into electric energy and dispose of the resulting electric energy by making it available in the engine-plant electric power mains. In other words, any universally applicable power-recovery system of testing must be an electric system, and it must have as its foundation of operation an electric generator of one form or another.

In Figure 2, the generator is a synchronous machine. It is equipped with an amortisseur winding and is started and connected to the line by being started at full voltage in the conventional synchronous-motor manner. During starting the coupling is set to transmit no torque, so that the synchronous machine starts unloaded, and the engine and engine half of the coupling remain at rest. After being connected to the line, the synchronous machine functions as a motor or generator, depending on whether mechanical energy is taken from it or delivered to it.

The synchronous machine operates at only one speed, synchronous speed, whereas the engine must operate over a range in speed. Furthermore, the synchronous machine will not control the loading of the engine. Within the limits of its capacity, it will deliver or absorb as much power as may be taken from it or delivered to it. Therefore, by itself, it will not meet two basic engine-testing requirements, namely, the control of the speed and the control of the torque at which an engine is loaded. Accordingly, the slip coupling is inserted, and it is the function of the slip coupling to provide these requirements.

The operation of this combination of synchronous machine and slip coupling is this: After the synchronous machine has been brought to speed and synchronized in the manner previously mentioned, the coupling is controlled to make the synchronous machine "turn over" the engine or run the engine mechanically. Then the engine is "fired," and after it is warmed up, it is brought, by its own power, to a speed above that of the synchronous machine. The synchronous machine, then acting as a generator, loads the engine on the power system by slipping in the coupling. The amount and the speed of this loading are controlled by the engine throttle and the degree to which the coupling is "energized."

It is of parenthetical interest that in this application the hydraulic slip coupling is used for the first time in a new role. Prior to its use here, it had been used either at low-slip values or on drives with rapidly decreasing power with increase in slip, such as fan drives. In the engine-loading application, the power to be transmitted through the coupling increases and increases rapidly with increase in coupling slip.

Engine and Test-Stand Loading

A power-recovery test stand designed for engine loading or generating requirements generally will have ample capacity for any corresponding motoring requirements. As a simple illustration of generator and coupling loading and power distribution between them, assume that an engine with the characteristic shown in Figure 4 is to be tested at speeds of 1,000 rpm, and 1,200 rpm, and that the synchronous machine has a speed of 720 rpm. Then, neglecting test-stand effi-

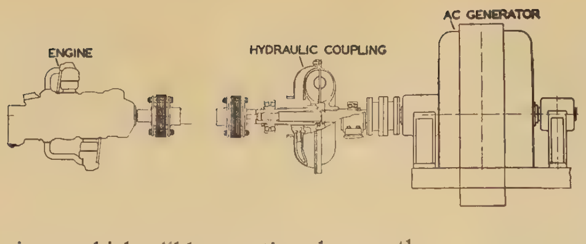


Figure 2 (left). Schematic view of the first power-recovery test stand

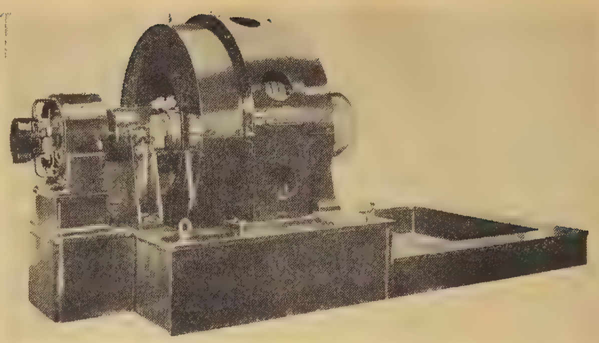


Figure 3 (right). Power-recovery test stand of the synchronous - generator type designed for the arrangement shown in Figure 2

The hydraulic coupling will be mounted on the base extension

ciency which will be mentioned presently, the power distribution is as follows:

Engine-Output Shaft (Propeller Shaft)		Coupling Loss*		Generator Loading**	
Speed (Rpm)	Horse-power	Horse-power	Kilo-watts	Horse-power	Kilo-watts
1,000	920	258	192	662	490
1,200	1,550	620	463	930	695

* Proportional to slip: $(1,000-720=280)/1,000\times 920=258$

** Inversely proportional to engine speed: $720/1,000\times 920=662$

Thus at 1,550 horsepower output of the engine, all of the 1,550 horsepower is not lost as is the case with systems of the load-dissipating type. The bulk of the power, 930 horsepower, is made available for useful purposes by the generator.

This distribution of engine output between power lost and power recovered is typical of all power-recovery systems discussed in this paper. That is, the power lost is proportional to the degree to which the engine operates above the synchronous speed of the generator. The difference between the engine output and the power lost is, again neglecting efficiency, the power recovered.

Figure 5, which is actual test data, illustrates more exactly the character of power distribution. Curve *A* is the engine output for a typical load run. *C* is the power returned to the power system through the generator. The difference between *A* and *C* is the total power lost and is made up of the true slip loss *A-B*, the same as previously mentioned, and the transmission loss *B-C*. This latter loss is made up of two parts, the losses in the slip coupling or equivalent device and the losses in the loading generator. Tests indicate that the transmission losses remain approximately constant for all loading conditions, and in value are approximately five per cent of rated engine output.

The ultimate interest from the standpoint of power recovery is, of course, generator horsepower hours or kilowatt-hours output. This involves the time factors in the engine-testing schedules and will be referred to presently. Before doing so, however, it will be helpful to know about the power-recovery developments that have been made since the original Pratt and Whitney Aircraft installation.

Power-Recovery Developments Since the First Power-Recovery Installation

In the fall of 1940, approximately six months after the original Pratt and Whitney Aircraft installation was placed in operation, the building of the first of three aircraft-engine plants to be erected by the automobile industry, using the power-recovery system of testing, was initiated. For the purpose of this paper, these engine plants can be designated as plant 1, plant 2, and plant 3.

For plant 1 the synchronous-machine hydraulic-coupling scheme of testing was adopted.

Work on plant 2 was started in the spring of 1941, and for this plant the same scheme of testing was adopted, except that magnetic slip couplings of the eddy-current type instead of hydraulic couplings were used. A modification in the testing scheme also was incorporated with these magnetic couplings in that they are arranged for braking below synchronous speed, as explained presently.

For plant 3, the most recent of the plants, another system of testing involving wound-rotor induction generators was developed to meet the requirements of this installation. These induction test stands will be described presently also.

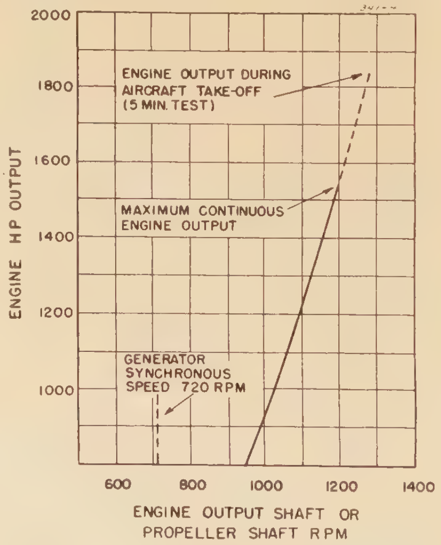


Figure 4. Curve to illustrate the fundamentals of engine horsepowers and speeds as discussed in text

At this writing there are now installed or on order for these various locations a large number of power-recovery testing equipments through which aircraft-engine production is or will be pushed. This total is a growing percentage of the total of all types of stands installed since the inception of the power-recovery system of testing. The equipments are made up of Westinghouse, Allis-Chalmers, or General Electric generators and, where used, hydraulic coupling division of American Blower Corporation hydraulic couplings or Dynamatic Corporation magnetic couplings.

Braked Magnetic-Coupling Synchronous-Machine Test Stand

The synchronous machine of Figure 2 has a relatively high synchronous speed, a speed that is in the upper reaches of the engine speeds. This high speed is selected in order to minimize the loss in the coupling which, as already stated, increases with increase in the difference between the engine and generator speeds. Also, as already stated, the engine cannot be loaded on the generator below synchronous speed, since at any engine speed below synchronous speed the synchronous machine will act as a motor. That is, the slip coupling is a speed stepping-down and not a speed stepping-up device. Therefore, the selection of a high generator speed presupposes that an engine is ready to operate at this high speed before it is required to be loaded; that is, it has been partially broken in by mechanical operation, possibly on the test stand itself, or possibly on a separate electric-motor running-in stand.

At the plant 2 installation an engine can be loaded below generator speed by loading on the magnetic coupling. To permit this, the generator and generator half of the coupling are held stationary by means of a brake when the engine is operating be-

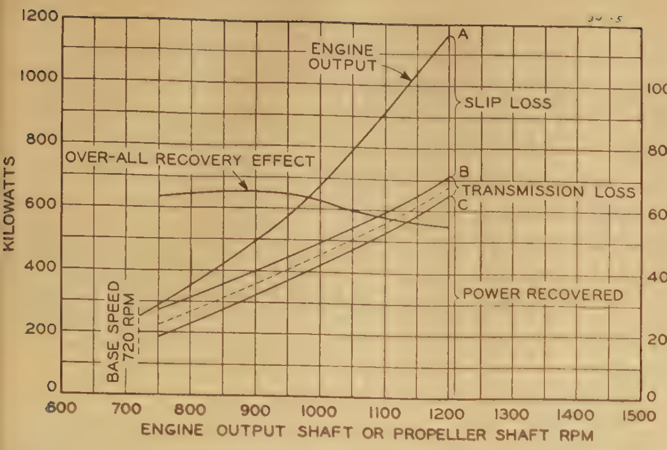
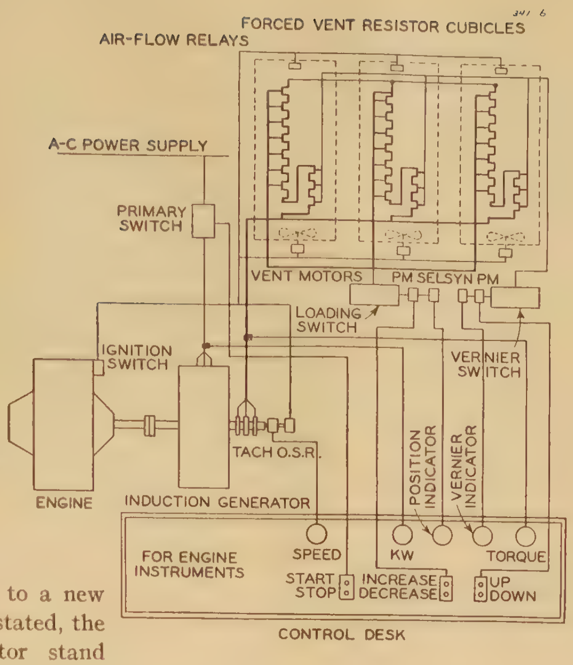


Figure 5. Test-cell performance showing typical distribution of power loss and power recovery for a power-recovery test stand

Figure 6 (right). Schematic arrangement of induction generator power-recovery stand



low synchronous speed. In other words, below synchronous speed the magnetic coupling functions in the same manner as any eddy-current loading machine, and above synchronous speed, with brake released and generator operating, it functions as a slip coupling. The magnetic coupling is water cooled and, designed for the slipping operation, has inherently sufficient capacity for the braked operation. The fact is that the loss of power under the braked condition of operation is low. This is so, because the engine outputs at low speeds are low, and the duration of the loading runs at low speeds are comparatively short.

Induction-Generator Power-Recovery System

Thus at the plant 2 installation a modification of the original power-recovery unit was made which permitted loading an engine below synchronous speed by the addition of a brake. For the plant 3 installation, still broader test-stand requirements were set up. For this installation it was desired not only to load the engine over its entire speed range but also to load the same engine in this manner when equipped with gear heads having three different ratios of speed reduction and consequently three different engine-output shaft speeds. This requirement made necessary a test stand of very considerably greater range of speed than previously used with the synchronous-machine slip-coupling stands, approximately 4:1 as compared with 1.5:1 or 2:1. Also, in this installation it was desired to eliminate the necessity of dissipating losses by means of cooling water, a requirement which assumes increased importance in the light of the increased speed range of operation and consequently increased slip loss.

These requirements pointed to a new type of stand, and, as already stated, the wound-rotor induction-generator stand was developed accordingly. Figure 6 shows schematically the fundamentals of the induction-generator stand.

As is well known, the induction generator is a variable-speed machine with inherent characteristics similar to those of the conventional wound-rotor induction motor. In its application to the test stand, the electrical design of the induction machine is such that its synchronous speed corresponds approximately to the minimum loading speed of the engine. When the generator is connected to the power system and driven by the engine at any speed above its normal synchronous speed, the generator will pump part of the engine output into the power system as recovered power and remainder into the loading resistor as lost power. The proportions of these powers are the same as for synchronous-machine slip-coupling stand.

With this wound-rotor induction-type stand the sequence of operation will be somewhat as follows: The first operation consists of a cold start and run-in test for testing mechanical operation and lubrication of the engine, the same as with the synchronous stand. This is done by starting up as an induction motor by secondary resistance control. After starting, speed is gradually increased up to approximately synchronous speed of the motor. When ready to "fire," the ignition switch is turned on and the engine throttle adjusted for approximate idling speed. When ready to load, the throttle is opened, and, as the generator speed passes through synchronism, load is applied automatically to the engine. By the proper manipulation of the loading resistor and the engine throttle, any desired values of load and speed are then established and maintained.

Figure 7 illustrates the nicety of speed and torque control available with this form of drive. In this figure the synchronous speed of the induction machine is 33 per cent of normal rated engine speed. The upper left-hand quadrant is the motoring range and the lower right-hand quadrant the generating range. The multipoint vernier control indicated is superposed on any one of the main loading points with the result that a very close speed and torque control is obtained over the entire range of operation.

Figure 8 shows the horsepower-loss and horsepower-recovery division of loading corresponding to the three gear ratios of Figure 7. With an engine passing through the usual test time and loading cycles the division of horsepower hours or kilowatt-hours recovered and lost would be somewhat as follows:

Engine Gear Ratio	Per Cent High (A)	 Medium (B)	 Low (C)
Kilowatt-hours recovered	58	47	37
Kilowatt-hours lost	42	53	63

Disposition of Power Loss With Induction-Generator Stand

One of the features of the induction-generator type of stand is the ease with which a high-loss loading element, the loading resistor, can be supplied, and the ease with which high-loss power can be dissipated. It will be noted from Figures 7 and 8 that, because of the wide range of operation, the amount of power to be dissipated is relatively considerable. For

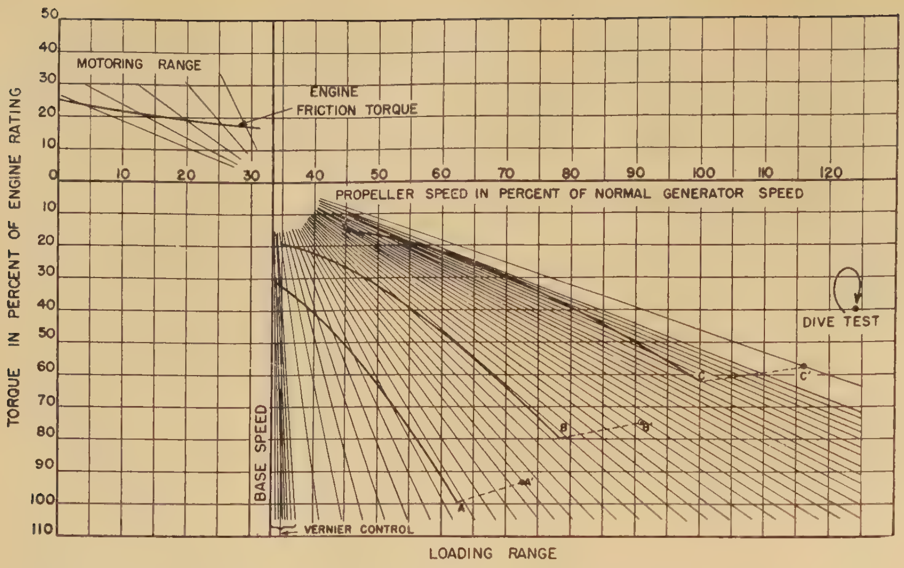


Figure 7. Torque-speed characteristics of induction-generator power-recovery stand designed for testing an engine with three different gear heads

The solid lines *A*, *B*, and *C* are normal loading ranges for high-, medium-, and low-gear ratios respectively. The points *A′*, *B′*, and *C′* are take-off loadings. The dive test point represents the highest speed to which the stand is required to operate

the more common present large engines equipped with gear ratios as previously discussed, the loading resistor would have a maximum continuous rating of approximately 1,200 kw. With the use of a resistor of the finned Calrod construction it is a relatively simple matter to blow this energy into the air. With such a construction no consideration need be given to the cost and availability of a suitable water-cooling or water-energy dissipating system.

Aircraft-Engine Testing With Propellers

The aircraft-engine industry has made testing developments of its own, so that a variety of testing schemes from which to choose are available to the designer of an engine-testing procedure and layout. But in the manufacture of the large engines produced today, the selection of a testing scheme probably would resolve into a choice between testing with propellers similar to aircraft propellers and testing with power recovery.

Figures 10 and 11 illustrate typical physical arrangements for these two schemes of testing. It will be noted that the structures shown differ fundamentally in that with propeller testing the structure must be designed to accommodate the air stream set up by the engine loading propeller, whereas with the power-recovery system this is not necessary, because the air stream is not present.

For the rest of this paper, the statements made may be scrutinized with the view that, if power-recovery testing were not used, propeller testing would be.

Recovery of Power

If one knows the engine sizes and rates of production and the horsepower-speed-torque characteristics of the testing cycles it is a simple matter to calculate the expected recovery of energy. These calculations will predict minimum or pessimistic expected results, however, because they will not take into account tests that have to be repeated for one reason or another, tests called penalty runs. For the purpose here, therefore, it is preferable to use actual test data and records.

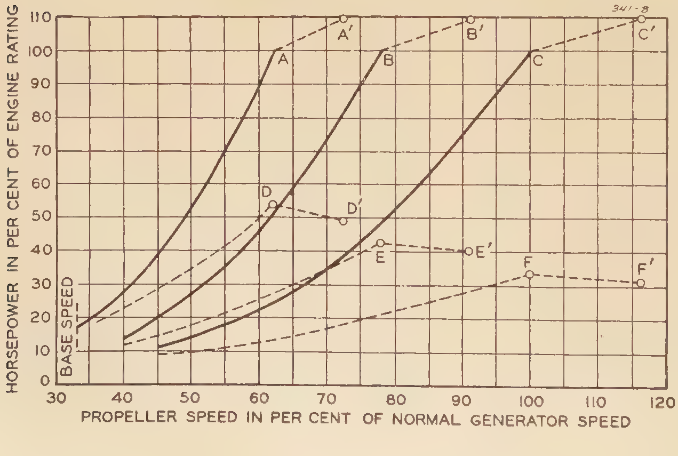


Figure 8. Curves showing the division between horsepower lost and horsepower recovered for the conditions of operation shown in Figure 7

For example, the area under curve *A* is engine output, and that under corresponding curve *D* is power recovered. The difference between the two is the power lost

From the standpoint of individual engines, records show that, from each engine of large rating coming off an assembly line, there may be recovered anywhere from 3,000 to 6,000 kilowatt-hours of energy.

From the standpoint of over-all engine-manufacturing-plant operation, Figure 9 is indicative of what may be expected. This figure is the plotting of the over-all performance of one of the new engine plants for a period of 72 consecutive hours. The plotting is actual normal plant performance made from instrument readings taken at the plant for the period indicated. The top curve shows the plant power consumption. The bottom curve shows the power purchased from the connected public utility. The difference between the two is the power recovered from the power-recovery test stands. It will be noted that this recovered power is more than half that required to operate the plant.

Table I. Record of Percentage of Total Engine-Plant Power Consumption Supplied by Power-Recovery Testing for a Large Engine Plant During a Period of Six Months

Month	Per Cent Recovery
1	54.6
2	57.5
3	53.0
4	53.7
5	59.6
6	57.3

Table I is similar over-all plant performance for a period of six months and shows again power recovery exceeding half of the total power required to operate the plant during that period.

Reverting to Figure 9, it will be seen that power-recovery testing also reduces power demand and consequently purchased power-demand charges. Without power-recovery testing, demand charges would be based on the top instead of the bottom curve. Obviously, also, the capacity of interconnecting apparatus such as transmission lines and transformers would have to be based on the top instead of the bottom curve.

The power-recovery system saves energy, reduces demand charges and interconnecting apparatus capacity, and does all this to an important degree. A propeller test stand will not do any of these

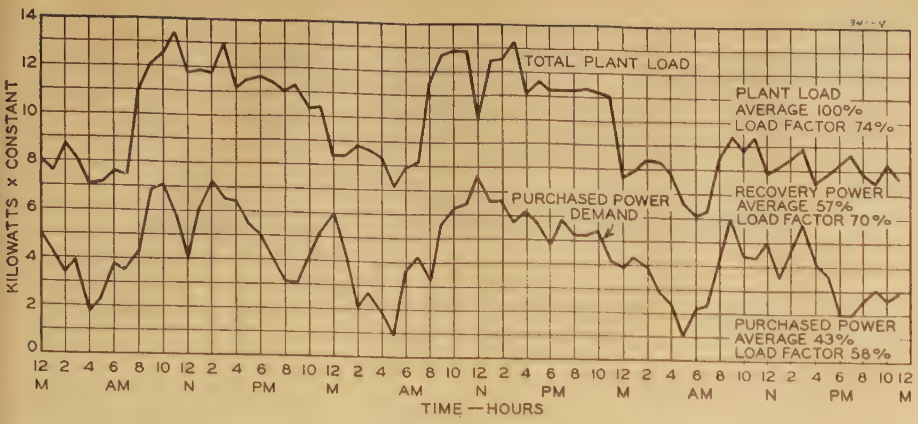


Figure 9. This 72-hour total plant power chart shows plant load, power recovered from the test stands, and purchased power

The area between the two curves represents the power supplied to the plant by the power-recovery test stands. In this record, 57 per cent of the total plant load is secured by power recovery and 43 per cent is purchased from the connected power company

things. These are important accomplishments that are of direct interest to the engine builder.

Measurement of Engine Loading

Besides the advantages enumerated at the outset of this paper, the power-recovery system of testing carries the important advantage that it is still open to expansion and improvement. As the paper shows, this system of testing is very new. Its full possibilities have not yet had time to develop.

One such possibility is the accurate measurement of engine loading by electric instrument measuring techniques. When the mechanical energy output of the engine has been converted into electric energy, the basis for such measurement is present. This is an important asset of the power-recovery system of testing that is open to the engine builder. It is an involved subject, however, and entails departure from aircraft-engine testing procedure of long standing.

Cost of Power-Recovery Testing Equipment

When all pertinent considerations are taken into account, the cost picture surrounding power-recovery testing is particularly favorable. The question from a cost standpoint is: how much more does a power-recovery system cost than a system in which power is not recovered, for example, the propeller system? In other words, to justify any greater cost for power-recovery testing, it is necessary only to justify the difference between the cost of power-recovery testing and such another system as may be contemplated.

The cost of the test-stand equipment required for a large engine power-recovery stand will likely be between $20,000 and $30,000. This equipment cost is largely offset, however, by the simpler building structure permissible with the power-recovery system. Those that are in a position to know building cost differences and such other cost differences that enter into over-all cost computations have submitted cost differences ranging from nothing to $15,000 per test stand for a power-recovery testing system of the capacity being discussed in this paper.

Assume that the difference standing against power-recovery testing is $15,000. Assume likewise that 1.5 engines per day per stand per 300-day year are tested at a saving in kilowatt-hours per engine of 4,000. Then with power at $0.004 per kilowatt-hour the annual saving is $7,200. The corresponding amortization rate is slightly over two years. In other words, the cost difference is eliminated in slightly over two years, and thereafter the savings continue indefinitely. No nonrecovery testing system can present such an outlook from an investment standpoint.

Furthermore, the preceding cost picture is not the best that can be presented. It does not take into account reduction in power-demand charges. Likewise, it does not take into account the possibility of testing engines more quickly by power-recovery and consequently the possibility of installing fewer stands for a given plant engine output.

Use of Critical Materials

In these times, the use of the critical materials necessary with power-recovery testing is, of course, a consideration of first line importance. New installations of power-recovery testing could not be advocated without a favorable critical-materials situation.

Figure 10. Schematic section of a modern propeller test stand

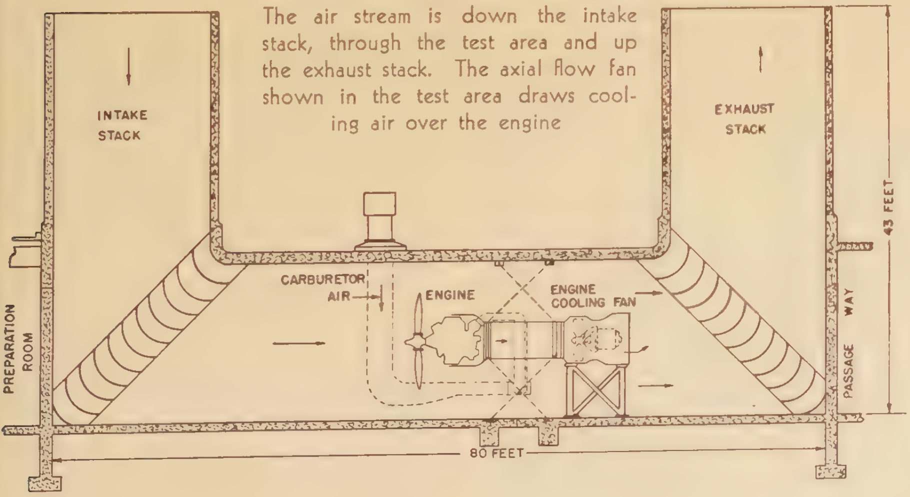
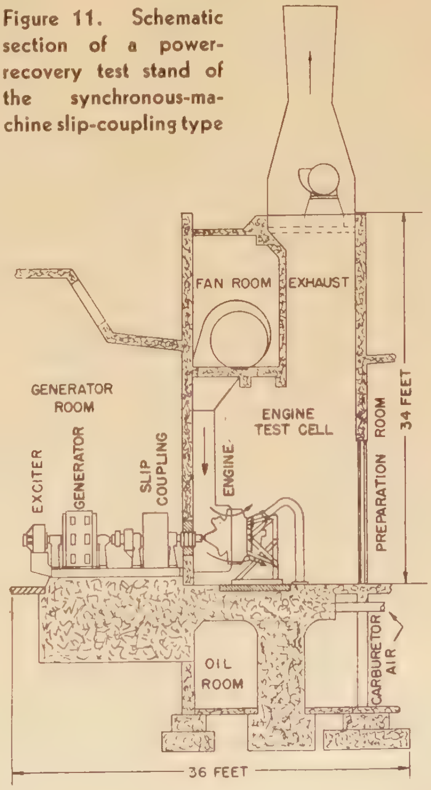

The air stream is down the intake stack, through the test area and up the exhaust stack. The axial flow fan shown in the test area draws cooling air over the engine

Figure 11. Schematic section of a power-recovery test stand of the synchronous-machine slip-coupling type



The approach to the critical-materials problem is in the fact that the use of critical materials is necessary to generate and supply power to any aviation engine plant, regardless of what means of securing power may be used. If the power-recovery system of testing can supply power with less use of such materials, its adoption should be justifiable, in fact, advocated.

Table II shows a comparison between the critical materials involved in obtaining a kilowatt of load capacity from power-recovery equipment and from a medium-size steam-generating plant:

Table II

Power Recovery	
Critical materials pounds per kilowatt	
Copper	10
Iron and steel	90
Steam Plant	
Critical materials pounds per kilovolt-ampere	
Copper	50 to 70
Iron and steel	250 to 400

The data show that power recovery is very much favored. Unless the circumstances surrounding a specific engine plant are very unusual, the conclusion is evident that it is in the best interest of the war effort to advocate the installation of generating capacity right at the engine plant by power recovery rather than to advocate the installation of an equivalent capacity in a distant steam station.

The Future of Power-Recovery Testing

The advantages of the power-recovery system as applied to the testing of aircraft engines have not yet had time to be appreciated universally. The statements made in the foregoing recital are simple engineering facts, easily checked, and they all point to the conclusion that power-recovery testing has intrinsic value that cannot be passed by lightly. Power-recovery testing has advantages that cannot be secured from any other form of testing.

But, in the final analysis, it is the engine builder and not the electrical engineer who will point the way and decide to what further extent power-recovery testing will be used. It may be quite possible that with the passing of time the engine builder will point with more and more favor, because he is already pointing toward larger aircraft engines—and the larger the engine the more advantageous power-recovery testing becomes.

Estimating Electric Loads for Military-Training Bases, Federal Housing, and Other Wartime Projects

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I. Method of Estimating Loads

At present a great amount of engineering talent is being utilized in the electrical planning and designing of various types of military-training bases, federal-housing projects, and other wartime projects. The Bonneville Power Administration has been requested to supply electric power to many types of war projects and of course was confronted with the problem of estimating the power requirements of each.

This paper is intended to show the methods used in estimating load requirements for various types of military and other wartime projects; to present certain conclusions pertaining to the resulting demand factors and load factors; to present test information on which barracks lighting and cooking loads are estimated; and, finally, to set forth certain derived data illustrating the effect of electric cooking on the total load.

Table I. Summary of Derived Information From Load Estimates of Military Training Bases

	⊖ Army Training Base*	Section II Navy Training Base**	Section III Air Training Base†	Section IV Lighter-Than-Air Station‡
Number of men	35,000	30,000	3,400	1,000
Total connected kilowatts	26,829	15,929	2,598	4,939
Average monthly maximum kilowatts	6,394	4,890	1,180	2,065
Annual maximum kilowatts	7,726	5,800	1,340	2,425
Demand factors (per cent)⊗				
Barracks and mess-hall lighting	41.5	40	50.4	56
Total load	23.8	31	45.4	42
Load factors (per cent)⊗				
Barracks and mess-hall lighting	54.4	56	54.6	56
Total load	74.8	73	69.4	67
Diversity between major load groups (per cent)⊗	1.48	1.27	1.14	1.08

* Complete electric cooking on one-sitting basis. 12,885-kw connected cooking load.

** Electric cooking on three-sitting basis. 4,481-kw connected electric-cooking load.

† Electric cooking on three-sitting basis. Large loads in shop motors and hangar lights.

‡ Approximately one half of total connected load in shop power and helium-repurification-system motors. Large load in hangar lighting.

⊗ Based on a typical average day.

⊖ See reference 2.

Detailed load estimates of various types of military bases and other wartime projects in the following sections of the paper, are presented as examples from which certain conclusions are drawn.

The general method used is to portray for each hour or half-hour period of the day the expected fluctuation for the various classes of load. The detailed connected load is first segregated into groups which have similar characteristics as to the time and nature of their uses. The barracks lighting, mess-hall lighting, and single-phase kitchen power are first segregated into one load group. This is done in order that the demand factors and daily load fluctuations of this type of load may be estimated by making use of the test information shown in the author's article entitled "Lighting and Cooking Loads in Army Barracks."[1] The connected electric cooking load is next segregated into a single group so that the test information pertaining to this load may also be applied. In instances where the cooking is planned so as to prepare meals to be served on a two- or three-sitting basis or cafeteria style, the load peaks applicable to each meal are broadened to

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The author acknowledges the help of Claude R. Lester, acting chief of the rates and statistics section, Bonneville Power Administration.

Table II. Electric-Cooking-Load Characteristics Based on Detailed Load Estimates of Military-Training Bases⊕

	● Army Training Base	Section II Navy Training Base	Section III Air Training Base	Section IV Lighter-Than-Air Station
1. Connected cooking load	12,885	4,481	487	223
Per cent of total	48	28	19	4.5
2. Peak responsibility*				
Kilowatts⊖	2,576	1,150	126	50
Per cent	40.3	23.5	10.7	2.4
Watts per man	73.5**	38.3†	37.1‡	50.0⊗
3. Incremental cooking load				
Kilowatts⊖	1,152	1,084	88	50
Watts per man	32.9**	36.1†	25.8‡	50.0⊗
4. Ratio of kilowatt peak responsibility / connected cooking kilowatts (per cent)	20	26	26	22

* Demand of electric cooking load at time of the total peak.

** Based on 35,000 men.

† Based on 30,000 men.

‡ Based on 3,400 men.

⊗ Based on 1,000 men. These ratios high because of bakery and recreation building loads.

⊖ Includes estimated distribution and substation losses.

⊕ Based on a typical average day.

● See reference 2.

an extent consistent with their probable variation from the test curves.

The office, shop, and other daytime loads are next segregated into groups having similar characteristics, and the hourly fluctuations during the day are estimated for each group.

Night lighting of a steady nature such as street, protective, and flood lighting is considered as a group in which the full connected load will be used during all hours of darkness.

The motive-power load is likewise segregated into groups of similar nature, and the load fluctuation of each group is estimated independently. Shop power, major refrigeration power, special power such as helium-repurification-compressor power (section IV) are samples of groups frequently found. Water pumping is estimated on the basis of the design plans. This type of load ordinarily can be operated on an off-peak basis.

Tables I and II present the general results of detailed load estimates for the four types of military bases presented in sections II, III, and IV and for the large army cantonment, details of which appear in a second article by the author, entitled "Estimated Load for 35,000-Man Army Cantonment."[2]

Table I presents a comparison of important derived information on demands, demand factors, and load factors.

Table III

Total connected load	1,897 kw
Average monthly maximum demand	665 kw
Annual maximum demand	860 kw
Average monthly load factor	63 per cent
Annual load factor	49 per cent
Demand factor of total load	35 per cent
Diversity between major load groups	1.19

The first is the large army training base in which full electric-equipped kitchens are planned with meals to be served on a one-sitting basis.

The second is a large naval training station in which the cooking is to be done electrically, with meals to be served on a three-sitting basis. Considerable steam-heated cooking equipment is used in this station.

Third is an air training base in which the hangars and miscellaneous power form the major part of the load.

The fourth is a naval lighter-than-air station in which the hangar lighting, shop power, and motive power for the helium repurification compressors form the principal loads. Motive power in this base is approximately one half of the total connected load.

The summary of the demand factors shows that the barracks lighting is about 40 per cent for the large bases. The smaller bases have higher demand factors of 50.4 per cent to 56 per cent. This is due to greater diversity being realized in the larger bases. The demand factors for the total load are reduced to 23.8 per cent for the large army base having electric cooking for meals on a one-sitting basis and 31 per cent for the large navy base with electric cooking for meals on a three-sitting basis. Demand factors for the total load of the air training base and the lighter-than-air station are 45.4 per cent and 42 per cent, respectively. These are types of bases in which the hangar and power loads predominate.

The load factors for the barracks and mess-hall lighting are fairly constant at 54.4 per cent to 56 per cent. The load factors for the total loads range from 67 per cent to 74.8 per cent, the higher load factors being expected for the large bases. The lighter-than-air station has the lowest estimated load factor, because the helium repurification process is assumed to be in use for only six days per month.

The estimated diversity between load groups is 1.48 for the large army base having electric cooking for meals served

Table IV. Summary of Cumulative Demand Factors for Typical Average Day

Groups	Types of Load	Connected Load (Kilowatts)	Estimated Maximum Kilowatts at Substation*	Cumulative Demand Factors (Per Cent)
1	Lighting and small power in living quarters and mess halls	4,064	1,612	40
1–2	Includes addition of general lighting and incidental heat and power	9,779	3,421	35
1–3	Includes addition of street lighting	9,979	3,504	35
1–4	Includes addition of pumping (off peak)	10,481	3,504	33
1–5	Includes addition of motive power	11,448	3,806	33
1–6	Includes addition of electric cooking and baking	15,929	4,890	31

* Includes estimated ten per cent distribution and substation losses.

Table V. Summary of Cumulative Load Factors for Typical Average Day

Groups	Types of Load	Energy Use (Kilowatt-Hours)*	Maximum Demand (Kilowatts)	Daily Load Factor (Per Cent)
1	Lighting and small power in living quarters and mess halls	21,790	1,612	56
1–2	Includes addition of general lighting and incidental heat and power	50,510	3,421	62
1–3	Includes addition of street lighting	53,150	3,504	63
1–4	Includes addition of pumping (off peak)	59,090	3,504	70
1–5	Includes addition of motive power	64,680	3,806	71
1–6	Includes addition of electric cooking and baking	85,720	4,890	73

* Estimated requirements at the substation, including ten per cent distribution and substation losses.

Table VI. Peak Responsibility of Various Groups of Loads for Typical Average Day

Group	Type of Load	Kilowatts*	Per Cent
1	Lighting and small power in living quarters and mess halls	1,540	31.5
2	General lighting and incidental heat and power	1,881	38.5
3	Street lighting	0	0
4	Pumping (off peak)	0	.0
5	Motive power	319	6.5
6	Electric cooking and baking	1,150	23.5
	Total of peaks	4,890	100.0
	Group Diversity = 6,188/4,890** = 1.27		

* Demands at time of estimated maximum demand for total load at the substation. Includes estimated ten per cent distribution and substation losses.

** Sum of peaks of individual groups (see Table VII).

on a one-sitting basis; 1.27 for the large naval training base with electrically cooked meals served on a three-sitting basis; 1.14 for the air training base in which the shops and hangars are operated continuously day and night; and 1.08 estimated diversity between load groups in the lighter-than-air base in which the hangars, shops, and helium repurification system are all operated on a 24-hour per day basis.

In Table II certain derived information on the electric cooking loads in each of the four bases is presented.

It is to be noted particularly that the connected cooking load for the large army base is nearly three times that of the navy base of approximately the same number of men in size. This is chiefly due to the effect of meal planning on one- and three-sitting bases, respectively.

In each of the load estimates shown, the ratio of the cooking demand at the time of the total peak for the training base (peak responsibility) is always less than the ratio of the connected cooking load to the total connected load. As an example, the connected cooking load of the 35,000-man army base is 12,885 kw or 48 per cent of the total connected load, whereas the cooking demand at the time of the total peak is 2,576 kw or only 40.3 per cent of the peak load.

The peak responsibility of the cooking load in watts per man is 73.5 for the army base, while it is only 38.3 watts per man for the similar-sized navy base. The principal reason for this difference is the planning of the meals on a one-sitting basis in the army base and a three-sitting basis in the navy base.

The incremental cooking load per man varies from 25.8 watts to 36.1 watts. The figure of 50 watts per man for the lighter-than-air station is due to a majority of the total cooking load being com-

Table VII. 30,000-Man Naval Training Station—Estimated Loads for Typical Average Day

Hour	Annual Maximum Day	Lighting and Small Power in Living Quarters and Mess Halls	Annual Maximum	General Lighting and Incidental Heat and Power	Annual Maximum	Street Lighting	Total Lighting and Miscellaneous	Pumping (Off Peak)	Annual Maximum	Motive Power	Total Lighting and Power	Annual Maximum	Electric Cooking and Baking	Annual Maximum	Total Load At Point of Use	Total Load Annual Maximum	Total Load At Substation†
Connected Load (Kilowatts)		4,064		5,715‡		200	9,979	502		967‡	11,448		4,481‡		15,929		
12–1 a. m.		245		320		200	765	300		85	1,150		305		1,455		1,600
1–2		245		320		200	765	300		85	1,150		305		1,455		1,600
2–3		245		320		200	765	300		85	1,150		305		1,455		1,600
3–4		245		320		200	765	300		85	1,150		602		1,770		1,945
A.M.–5		325		320		200	845	300		85	1,230		1,385		2,615		2,875
5–6		1,000		320		200	1,520	300		275	2,095		1,325		3,420		3,760
6–7 a.m.		1,260		410		100	1,770	300		275	2,345		1,295		3,640		4,005
7–8		1,180		750			1,930	300		275	2,505		820		3,325		3,660
8–9		1,020		1,415			2,435	300		275	3,010		950		3,960		4,355
9–10		895		1,865			2,760	300		265	3,325		1,120		4,445		4,890
10–11		815		1,865			2,680	300		245	3,225		1,100		4,325		4,760
11–12 noon		815		1,865			2,680	300		245	3,225		1,115		4,340		4,775
12–1 p.m.		855		950			1,805	300		305	2,410		610		3,020		3,320
1–2		815		1,865			2,680	300		285	3,265		710		3,975		4,375
2–3		815		1,915			2,730	300		245	3,275		890		4,165		4,580
3–4	860	1,000	2,505	1,965			2,965		315	245	3,210	1,520	1,070	5,200	4,280	5,720	4,710
4–5	1,350	1,400	2,115	1,710			3,110		325	290	3,400	1,480	1,045	5,270	4,445	5,800	4,890
5–6	1,595	1,465*	1,715	975	100		2,440		345	310	2,750	1,510	1,065	5,265	3,815	5,790	4,195
6–7 p.m.	1,595**	1,465	2,040	1,300	200	100	2,865		325	295	3,160	1,110	760	5,270	3,920	5,800	4,310
7–8	1,390	1,220	2,395	1,765	200	200	3,185		305	275	3,460	960	740	5,250	4,200	5,775	4,620
8–9	1,270	1,140	2,380	1,665	200	200	3,005		240	210	3,215	605	485	4,695	3,700	5,165	4,070
9–10		610		1,120		200	1,930	300		160	2,390		420		2,810		3,090
10–11		490		470		200	1,160	300		95	1,555		345		1,900		2,090
11–12 m.		245		320		200	765	300		85	1,150		345		1,495		1,645
Total		19,810		26,110		2,400	48,320	5,400		5,080	58,800		19,130		77,930		85,720
Daily or monthly load factor (per cent)		56		55		50	63	75		68	71		58		73		73
Annual load factor (per cent)		52		43		50	51	75		61	57		41		62		62

* 4,064×51.5 per cent estimated demand factor×70 per cent estimated diversity = 1,465 kw maximum-demand, typical average day.

** 4,064×56 per cent estimated demand factor×70 per cent estimated diversity = 1,595 kw maximum-demand, annual maximum day.

† Includes estimated ten per cent distribution and substation losses.

‡ Estimated by separate details of various load elements in each group.

posed of night baking and ships service building loads, which probably will be used extensively at the time of the maximum demand for the base.

The most important conclusion relative to the electric cooking loads is the ratio of the peak responsibility to the connected cooking load. This is shown to vary from 20 per cent to 26 per cent in the four load estimates presented. The ratio of 20 per cent for the army base reflects cooking for meals on a one-sitting basis. This ratio is increased to 26 per cent for the training bases having meals served in three sittings. This difference is reflected by the longer duration of the peak periods and the reduced diversity of cooking on the three-sitting basis. Thus the general conclusion is that only one fifth to one fourth of the total connected cooking load in these types of training bases may be reflected as actual demands at the time of the maximum demand for the entire training base.

Estimated loads are shown in section V of the various types of living units contained in a large war housing project providing housing for 8,700 families and 4,000 single men. These types consist of apartments, dormitories, temporary demountable houses, and permanent houses. The over-all connected load is 87,764 kw, including allowances for pumping and commercial uses. The estimated maximum demand is 10,725 kw, resulting in an annual demand factor of 12.2 per cent for the entire project.

Table VIII. Summary of Cumulative Demand Factors for Typical Average Day

Service	Connected Load (Kilowatts)	Estimated Maximum Demand at Substation*	Demand Factor (Per Cent)
1. Lighting in barracks, officers' quarters, galleys, and so forth	568	286	50.4
2. Includes addition of lighting and power in hospital	782	390	49.9
3. Includes addition of lighting in administration building, offices, shops, and so forth	1,196	583	48.7
4. Includes addition of lighting in recreation building	1,448	676	46.7
5. Includes addition of lighting in hangars	1,638	885	54.0
6. Includes addition of lighting in streets, runways, flood lights, and so forth	1,688	940	55.7
7. Includes addition of motive power in shops, refrigerators, water pumps, and so forth	2,111	1,083	51.3
8. Includes addition of electric cooking in galleys	2,485	1,138	45.8
9. Includes addition of electric cooking in hospital and recreation building	2,598	1,180	45.4
Diversity = 1,350/1,180 = 1.14			

* Includes estimated ten per cent distribution losses.

Table IX. Summary of Cumulative Load Factors Based on Typical Average Day

	Energy* (Kilowatt-Hours)	Demand* (Kilowatts)	Daily Load Factor (Per Cent)
1. Lighting in barracks, officers' quarters, galleys, and so forth	3,750	286	54.6
2. Includes addition of lighting and power in hospital	5,030	390	53.7
3. Includes addition of lighting in administration building, offices, shops, and so forth	8,140	583	58.2
4. Includes addition of lighting in recreation building	8,820	676	54.4
5. Includes addition of lighting in hangars	12,790	885	60.2
6. Includes addition of lighting in streets, runways, flood lights, and so forth	13,560	940	60.1
7. Includes addition of motive power in shops, refrigerators, water pumps, and so forth	17,000	1,083	65.4
8. Includes addition of electric cooking in galleys	18,740	1,138	68.6
9. Includes addition of electric cooking in hospital and recreation building	19,660	1,180	69.4

* Includes estimated ten per cent distribution losses.

A short discussion follows on the estimated loads, demand factors, and load factors for a typical army hospital and an army reconsignment depot. Similarity may be noted as to those factors as compared with the other types of bases discussed.

Army Hospital

Plans for a typical army hospital designed for 1,000 beds on a general 1,500-bed plan were analyzed. Living quarters were planned for an operating personnel of 668 men. Complete electric cooking was provided for in the nurses', officers', patients', and medical-detachment messes and in wards, post exchange, and recreation buildings.

Table X. Peak Responsibility of Various Groups of Services for Typical Average Day

	Kilowatts*	Per Cent
1. Lighting in barracks, officers' quarters, galleys, and so forth	275	23.3
2. Lighting and power in hospital	115	9.7
3. Lighting in administration building, offices, shops, and so forth	192	16.3
4. Lighting in recreation building	66	5.6
5. Lighting in hangars	208	17.6
6. Lighting of streets, runways, flood lights, and so forth	55	4.7
7. Motive power for shops, refrigeration, pumps, and so forth	143	12.1
8. Electric cooking in galleys	82	7.0
9. Electric cooking in hospital and recreation building	44	3.7
Total	1,180	100.0

* Based on estimated maximum demands at substation for maximum demand period of 6 to 7 p.m

The results of this detailed load estimate, which was performed in the same general manner used in other studies presented in this paper, are shown in Table III.

In this hospital the connected electric cooking equipment was 1,048 kw, which accounts for 55 per cent of the total connected load of the hospital. The peak responsibility was estimated to be 319 kw, or 48 per cent of the total maximum demand for a typical day. The ratio of the kilowatts of peak responsibility to the connected cooking load is estimated to be 30 per cent.

RECONSIGNMENT DEPOT

This is an army depot which is built for the purpose of receiving shipments of all types of supplies by rail and then regrouping and reshipping the supplies to various destinations. The major part of the labor will be done by civilians, most of whom will live outside the depot.

The total connected load was found to be 1,293 kw, the major portion being lighting and space heating in warehouses. A limited amount of housing for civilians is provided.

Electric cooking equipment is provided in a cafeteria, an officers' mess, and a civilian mess hall.

The estimated average monthly maximum demand was 565 kw, which results in a demand factor of 44 per cent. The annual maximum demand is estimated to be 815 kw.

Thus the method used in estimating the types of loads described in this paper

Table XI. Naval Air Training Base—Estimated Load for

Group Number		1	2		3		4	5		6		7
Type of Load	Annual Maximum	Lighting in Living Quarters	Lighting and Power in Hospital	Annual Maximum	Lighting in Offices, Shops, and so forth	Annual Maximum	Lighting in Recreation Building	Lighting in Hangars	Annual Maximum	Night Lighting (Streets, Flood, and so forth)	Total Lighting	Motive Power
Hour / Connected Load (Kilowatts)		568	214		414◐		252†	190‡		50	1,688	423⊗
12–1 a.m.		45	20**		80			190		50	385	130
1–2		45	20		80			190		50	385	130
2–3		45	20		80			190		50	385	130
3–4		45	20		80			190		50	385	130
4–5		60	20		80			190		50	400	130
5–6 a.m.		150	20		80			190		50	490	130
6–7		230	20		85		20	150		50	555	130
7–8		210	50**		155		20	100		25	560	130
8–9		175	50		135		20	100			480	130
9–10		160	50		120		20	100			450	130
10–11		145	50		120		20	100			435	130
11–12 noon		145	50		120		20	100			435	130
12–1 p.m.		150	50		120		20	100			440	130
1–2		145	50		120		20	100			435	130
2–3		145	50		120		20	100			435	130
3–4		150	50		135		20	100			455	130
4–5		200	80		155		20	130			585	130
5–6 p.m.	285	260*	90	200	180	125	30	150	50	25	735	130
6–7	270	250	105**	200	175	125	60	190	50	50	830	130
7–8	240	220	105	200	165	125	125	190	50	50	855	130
8–9	220	200	105	200	150	125	125	190	50	50	820	130
9–10		110	50		130		60	190		50	590	130
10–11		80	20		80			190		50	420	130
11–12 midnight		45	20		80			190		50	385	130
Total		3,410	1,165		2,825		620	3,610		700	12,330	3,120
Daily load factor (per cent)		54.6	46.2		65.4		20.7	79.2		58.3	60.1	100

* 568 kw connected × 51.5 per cent demand factor from test information × 90 per cent estimated diversity for 36 buildings = 260 kw estimated maximum demand.

** Estimated 50 per cent of lighting on at maximum; 10 per cent at minimum; 25 per cent in day.

† 252 kw connected × 50 per cent estimate night = 125 kw; 10 per cent estimate day, or 20 kw.

‡ 190 kw connected × 100 per cent night, 50 per cent day.

⊗ 423 kw connected × 30 per cent all day.

⊖ Cooking on three-sitting basis, 374 kw con-

begins with the grouping of various types of connected loads. These groups are compiled on the basis of their application to test data available and their similarity and nature of probable time of daily use. Then, by estimating the fluctuations of each group over an average day, the maximum demands of the total of the groups may be approximated.

Sections II to V present details of the load estimates discussed.

II. Load Estimate for 30,000-Man Naval Training Station

The details of the estimated load for a 30,000-man naval training station are presented in Tables IV through VII.

This station has a planned connected load of 15,929 kw. The estimated maximum demand for a typical average day is 4,890 kw, and for an annual maximum day, 5,800 kw. The plans for this base called for electric cooking in the mess halls, subsistence buildings, ships service buildings, dispensary, and bakery. Some steam tables and cookers are planned in addition to the electric cooking equipment.

The demand factors range from 40 per cent lighting and small power in the living quarters and mess halls to 31 per cent for the total load (Table IV). The average daily load factors range from 56 per cent for lighting and small power in the living quarters and mess halls, to 73 per cent for the total load (Table V).

Table VII presents the basic details of the estimated loads of the various types found in this training station.

The maximum demand for the lighting and small power in living quarters and mess halls was based on test data previously mentioned, and is shown by footnotes on Table VII. Because a large number of barracks was being planned (160), the diversity would reduce the group demand to an estimated 70 per cent of the test demand factors for both the average and annual maximum days. The load fluctuation during the day was prorated on the basis of the aforementioned test curves.

The general lighting and incidental heating and power totals 5,715 kw of connected load. This load was segregated into smaller groups of similar characteristics, and the daily load fluctuations of each group were estimated independently. Included in this general group are various segregations such as lighting in offices. This load was estimated on the basis of a 60 per cent demand factor during the day, with small amounts being used at night. Lighting, heating, and small power normally used in the daytime, were estimated on the basis of 50 per cent of the connected load being used during the day, and approximately 10 per cent at night. The demand for the ships service and recreation buildings was estimated at 75 per cent of the connected load in the evening with small amounts used during the day. The evening and night lighting was estimated to be in use to the extent of 40 per cent of the connected load during the evening and late afternoon. This lighting is principally in the drill halls, auditorium, and bakery.

The street lighting was assumed to be used fully during the night hours.

The pumping power having a connected load of 502 kw was estimated to have a continuous demand of 300 kw and will be operated on an off-peak basis.

The motive power totalling 967 kw connected was likewise segregated into smaller groups of similar characteristics, and the fluctuations during the day of each group were estimated separately. Included in this group are loads for the incinerator and sewage plant, of which the sewage power is the major load. These were estimated at the full capacity at morning rising hours, 80 per cent after meal hours, and about 50 per cent at other hours. A group containing the day motive power was estimated at 25 per

Typical Average Day

Total Lighting and Power	Annual Maximum	8 Electric Cooking (Galleys)	9 Electric Cooking (Hospital and Recreation Building)	Annual Maximum	Total Load At Buildings	Total Load At Substation	Annual Maximum
2,111		374◐	113⊕		2,598	●	
515		15	10		540	590	
515		15	10		540	590	
515		15	30		560	620	
515		50	50		615	680	
530		130	60		720	790	
620		125	60		805	890	
685		120	40		845	930	
690		75	30		795	870	
610		85	45		740	810	
580		105	50		735	810	
565		100	45		710	780	
565		95	25		685	750	
570		50	20		640	700	
565		65	25		655	720	
565		85	45		695	760	
585		100	40		725	800	
715		95	35		845	930	
865	130	90	45	1,205	1,000	1,100	1,330
960	110	75	40	1,220	1,075	1,180	1,340
985	60	40	40	1,140	1,065	1,170	1,250
950	20	15	40	1,080	1,005	1,110	1,190
720		15	40		775	850	
550		15	20		585	640	
515		15	10		540	590	
15,450		1,590	855		17,895	19,660	
65.4		51.0	59.4		69.4	69.4	

nected × 35 per cent demand factor = 130 kw maximum demand.

⊕ 50 per cent × 113 kw connected = 60 kw maximum same fluctuation as one mess barracks; evening

loads held high because of cooking in recreation building of 57 kw connected.

● Includes estimated 10 per cent distribution losses.

◐ Based on separate detail of smaller load groups.

cent demand factor during work hours, with small amounts being used at night. This load is principally laundry and shop power. The evening motive power, consisting chiefly of ventilating and heater fan motors in recreation buildings, mess halls, and ships service buildings, was estimated to be used at a 50 per cent demand factor during evening hours and during the breakfast period. The refrigeration motors for the cold-storage plant having a connected load of 153 kw were assumed to have a demand of 50 per cent of the connected load during the day hours, decreasing to 10 per cent during the cooler night hours.

The connected load for the electric cooking and baking was also segregated into smaller groups, and the daily load fluctuation of each group was analyzed separately. The cooking in mess halls and subsistence buildings is planned so as to prepare meals to be served on a three-sitting basis. The demand factors found by the test data of 35 per cent for the typical day and 50 per cent for the maximum day are used to determine the maximum demand. No diversity factors are used in this instance, as a total of only nine mess halls and subsistence buildings are planned. The fluctuation in cooking load during the day is based on the test curves in general, except that, because of the meals being served in three sittings, the cooking periods are assumed to be of longer duration. Included in this group is the bakery which is planned to be operated at night. The maximum demand is assumed to be 95 per cent of the connected load of 231 kw during the "warming-up" period at the start of the bakers' shift. During the rest of the night baking period, the demand is estimated to be 75 per cent of the connected load.

III. Load Estimate for Naval Air-Training Base

This base has a connected load of 2,598 kw. The estimated maximum demand for a typical day is 1,180 kw, and for an annual maximum-demand day, 1,340 kw.

The base is for the purpose of training navy pilots and is planned for an ultimate total of 3,400 officers, instructors, and trainees. There is planned a 952-man electric-equipped mess hall for the enlisted men and a separate mess hall for the officers. Three hangars, an assembly and repair shop, and a recreation building are other major loads.

The demand factors range from 50.4 per cent for the lighting in the living quarters and mess halls to 45.4 per cent for the total load. The average daily load factors range from 54.6 per cent for lighting in the living quarters and mess halls, up to 69.4 per cent for the total load.

Table XI contains the basic details for the estimated fluctuations in each of the various loads during a typical day. The same general procedure was used as in the preceding section II.

The electric cooking is planned for meal service on a three-sitting basis, as the 952-man mess hall must feed about 3,000 men. The demand factors used are based on the test information previously mentioned. The load fluctuation during the day is based on the test curves in general, except that because of longer meal periods the peaks are estimated to be of longer duration than shown by the test data.

The maximum demands and daily fluctuation of the other types of loads are explained by means of footnotes on Table XI.

Tables VIII through XI present details of the load analysis.

IV. Load Estimate for a Naval Lighter-Than-Air Station

This base has a designed connected load of 4,939 kw. The estimated maximum demand for a typical day is 2,065 kw, and for an annual maximum day, 2,425 kw.

The base is to be used to house, repair,

Table XII. Summary of Cumulative Demand Factors for Typical Average Day

	Connected Load (Kilowatts)	Estimated Maximum Kilowatts Demand at Substation*	Cumulative Demand Factors (Per Cent)
1. Lighting in living quarters	508	286	56
2. Includes addition of miscellaneous lighting in offices, hospital, recreation building, storehouses, and so forth	989	424	43
3. Includes addition of hangar office and shop lighting and heating	2,724	1,342	49
4. Includes addition of street and flood lighting	2,761	1,380	50
5. Includes addition of shop and miscellaneous power	4,091	1,766	43
6. Includes addition of electric cooking and baking	4,314	1,815	42
7. Includes addition of helium compressors, exhausters, and air-compressor motive power	4,939	2,065	42
Diversity = 2,228/2,065 = 1.08			

*Includes estimated ten per cent distribution and substance losses.

Table XIII. Summary of Cumulative Load Factors for Typical Average Day

	Kilowatt-Hours*	Demand*	Daily Load Factor (Per Cent)
1. Lighting in living quarters	3,828	286	56
2. Includes addition of miscellaneous lighting in offices, hospital, recreation building, storehouses, and so forth	7,056	424	69
3. Includes addition of hangar office and shop lighting and heating	22,693	1,342	70
4. Includes addition of street and flood lighting	23,160	1,380	70
5. Includes addition of shop and miscellaneous power	31,433	1,766	74
6. Includes addition of electric cooking and baking	32,593	1,815	75
7. Includes addition of helium compressors, exhausters, and air-compressor motive power	38,530	2,065	78

* Estimated requirements at substation including ten per cent distribution losses.

Table XIV. Peak Responsibility of Various Groups of Services for Typical Average Day

Service	Kilowatts*	Per Cent
1. Lighting in living quarters	220	10.7
2. Miscellaneous lighting in offices, hospital, recreation building, storehouses, and so forth	165	8.0
3. Hangar office and shop lighting and heating	957	46.3
4. Street and flood lighting	39	1.9
5. Shop and miscellaneous motive power	385	18.6
6. Electric cooking and baking	50	2.4
7. Helium compressors, exhausters, and air-compressor motive power	249	12.1
Total	2,065	100.0

* Based on maximum demand period of 8–9 p.m. Demands estimated at substation.

and maintain powered balloons for coast patrol service. The personnel needed for the operation of the base is only about 1,000 men. The principal type of load at this base is hangar lighting, shop power, and power for a helium repurification system. There are two hangars planned, each about 1,050 feet in length and capable of housing six patrol balloons in each hangar. The balloons are approximately 70 feet in diameter and 250 feet long. Shops and offices are located along the sides of the hangars.

Cooking will be done electrically in a 696-man enlisted men's mess and in a 252-man officers' mess. The baking will be done electrically, and electric cooking is planned in the recreation building and hospital.

The estimated demand factors (Table XII) for this base range from 56 per cent for the barracks lighting, to 42 per cent for the total load. The estimated daily load factors range from 56 per cent for barracks lighting up to 78 per cent for the total load on days when the helium repurification system is being operated. On an average monthly basis the load factor is estimated at 67 per cent.

Details of the load estimate are shown on Table XV. The manner of making the estimates for the various types of loads is substantially the same as in the other load estimates given in this paper. There are, however, two or three loads of a somewhat special nature.

One of these is 330 kw of connected lighting in the main part of each hangar that will be used continuously. It is estimated that the full connected load will be used at night, and 30 per cent of the connected load will be used during the day.

Another special load is shop power located in the hangars and having a total connected load of 950 kw (included in the power group) planned to be used on a three-shift basis. An estimated demand factor of 25 per cent was used to estimate the loads throughout the day, except that at meal times and shift changes the load is expected to be lower.

The connected loads for the helium repurification system and air-compressor motors total 625 kw. The helium re-

Table XV. Naval Lighter-Than-Air Station—Estimated Daily Fluctuation in Various Types of

Time of Day	Annual Maximum	Lighting in Living Quarters	Annual Maximum	Miscellaneous Lighting	Annual Maximum	Hangar, Office and Shop Lighting and Heating	Annual Maximum	Street and Flood Lighting	Total Lighting	Annual Maximum	Shop and Miscellaneous Power	Annual Maximum
Connected Load (Kilowatts)		508*		481**		1,735**		37†	2,761		1,330**	
12–1 a.m.		60		65		855		35	1,015		220	
1–2		60		65		855		35	1,015		345	
2–3		60		65		855		35	1,015		345	
3–4		60		65		855		35	1,015		355	
4–5		60		65		855		35	1,015		360	
5–6		150		80		860		35	1,125		360	
6–7 a.m.		225		80		580		20	905		235	
7–8		210		165		395			770		235	
8–9		180		165		385			730		355	
9–10		160		165		335			660		365	
10–11		145		165		335			645		360	
11–12 noon		145		165		335			645		235	
12–1 p.m.		150		100		260			510		225	
1–2		145		155		340			640		360	
2–3		145		155		335			635		360	
3–4		150		155		335			640		365	
4–5	240	200	170	165	505	335	0		700	265	240	95
5–6	250	260*	195	190	585	320	20		770	255	230	70
6–7	285*	245	170	140	840	525	35	20	930	400	350	50
7–8	250	215	180	150	840	825	35	35	1,225	400	350	45
8–9	225	200	180	150	835	870	35	35	1,255	400	350	45
9–10	110	110	125	110	875	860	35	35	1,115	395	345	40
10–11		85		80		855		35	1,055		345	
11–12 m.		60		75		855		35	1,025		230	
Total		3,480		2,935		14,215		425	21,055		7,520	
Load factor (per cent)												
Daily or monthly		56		64		68		51	70		90	
Annual		51		63		67		51	66		78	

† On during all dark hours.

* 508×51.5 per cent demand factor = 260 kw maximum (5–6 p.m.). Annual maximum 508×56 per cent = 285 kw maximum.

** Estimated on the basis of individual detail for each of the separate load groups.

purification system consists of three 75-horsepower motors for the high-pressure compressors and one 150-horsepower motor in each hangar for the low-pressure exhauster motors. There is also one 50-horsepower air-compressor motor in each hangar. The estimated load for the average day was based on the assumed use of one 150-horsepower exhauster motor and one 75-horsepower high-pressure compressor motor being used simultaneously. The load for the estimated maximum day is based on one 150-horsepower exhauster motor and all three 75-horsepower motors being used at one time.

The repurification process is performed in two stages. The impure helium is taken from the balloons at slightly more than atmospheric pressure and compressed by means of the low-pressure exhauster motor to a pressure of 60 pounds per square inch. Thence it is delivered to a large storage sphere which is about 60 feet in diameter. The helium gas is then put through purification processes which remove the carbon dioxide, nitrogen, and other impurities. The second stage is performed by the high-pressure compressors. These are three stage, tandem, reciprocating compressors which compress the helium to a pressure of 2,500 pounds per square inch and deliver it to the tank car. By means of by-passing one of the stages of the high-pressure compressor, the helium can be delivered at a pressure of 750 pounds per square inch directly to the storage tanks. The repurification process through the high-stage compressors takes about three days of constant running.

Tables XII through XV present details of the load analysis.

Loads, Typical Average Day

Electric Cooking and Baking	Total Lighting, Power, Heating, and Cooking	Annual Maximum	Helium Repurification and Air-Compressor Motors	Annual Maximum	Total: At Point of Use	Total: Annual Maximum	Total: At Substation
223**	4,314		625**		4,939		‡
30	1,265		225		1,490		1,640
30	1,390		225		1,615		1,775
30	1,390		225		1,615		1,775
50	1,420		225		1,645		1,810
80	1,455		225		1,680		1,850
70	1,555		225		1,780		1,960
50	1,190		225		1,415		1,555
40	1,045		225		1,270		1,395
50	1,135		225		1,360		1,495
60	1,085		225		1,310		1,440
50	1,055		225		1,280		1,410
40	920		225		1,145		1,260
35	770		225		995		1,095
40	1,040		225		1,265		1,390
55	1,050		225		1,275		1,400
50	1,055		225		1,280		1,410
35	975	425	225	1,700	1,200	1,870	1,320
25	1,025	425	225	1,800	1,250	1,980	1,375
45	1,325	425	225	2,205	1,550	2,425	1,705
50	1,625	425	225	2,175	1,850	2,390	2,035
45	1,650	425	225	2,195	1,875	2,415	2,065
35	1,495	425	225	2,005	1,720	2,205	1,890
30	1,430		225		1,655		1,820
30	1,285		225		1,510		1,660
1,055	29,630		5,400		35,030		38,530
55	75		19	(monthly—6 days per month)			
46	69		10	(annual)			

‡ Includes estimated ten per cent substation and distribution losses.

V. Load Estimate for a War Housing Project

In order to determine the loads to be expected from the various types of housing units included in this project, the plans were first examined in detail to ascertain the equipment to be used and the lighting outlets provided. This information was checked wherever possible with that actually installed.

Table XVI shows the connected loads and estimated annual maximum demands of each of the various groups of housing units. The annual maximum demand of the total project is estimated to be 10,725 kw at the main substation.

Table XVI

Type of Units	Number	Connected Load (Kilowatts): Per Unit	Connected Load (Kilowatts): Total	Estimated Annual Maximum Demands* at Time of System Peak: Per Unit**	Estimated Annual Maximum Demands* at Time of System Peak: Total
Apartments	4,000	2.59	10,337	0.39	1,562
Dormitories†	4,000 men	0.34	1,367	0.18	725
Temporary houses	4,000	15.5	62,000	1.29	5,170
Permanent houses	700	16.0	11,200	2.26	1,584
Pumping	6	143.0	860		660
Commercial, street lighting, and miscellaneous	lot		2,000		1,034
Total			87,764		10,725

* Including estimated ten per cent substation and distribution losses.

** Average on three-shift basis of work.

† Includes mess halls, electric kitchen, administration building, street lighting, and post office.

Apartments

The detail of the connected load on the 4,000 units of apartments is indicated in Table XVII.

The typical *A* unit (one-room) has two ceiling pull-chain outlets (200 watts), two duplex plug outlets (100 watts), one light switch and plug combined (200 watts), and a "Rangette" (1,650 watts), totaling 2.15 kw per apartment unit.

The typical *B* unit (two-room) has two pull-chain outlets (200 watts), four duplex plug outlets (200 watts), two light switches and plug outlets combined (400 watts), and a "Rangette" (1,650 watts), totaling 2.45 kw per apartment unit.

The service units are for laundry, bathrooms, heating plant, and storage rooms. The total lighting aggregates 3.6 kw connected. The power consists of ventilating motors aggregating 290 horsepower for the total of 150 service units.

Table XVII

Units	Type	Connected Load: Per Unit	Connected Load: Total
1,024	*A* (1-room)	2.15	2,202
2,976	*B* (2-room)	2.45	7,291
(150)	Service units		
	lighting	3.6	540
	power	1.93 (Avg.)	290
(70)	Street-lighting units	0.2	14
4,000 apartments		2.59	10,337

The average connected load per apartment, including service units and street lighting, is 2.59 kw, and the coincidental

Table XVIII

Lighting	0.8 kw	500 kilowatt-hours
Small appliances	1.0 kw	
Refrigerator	0.2 kw	250 kilowatt-hours
Range	11.0 kw	1,100 kilowatt-hours
Water heater	2.5 kw	3,000 kilowatt-hours
Total	15.5 kw	4,850 kilowatt-hours

demand at the time of the system peak averaged 0.39 kw per unit. The resulting demand factor is 15 per cent.

The war apartment units were first separated into three groups of units, each group representing the apartments to be used by each working shift. The relative number of apartments assigned to each shift was determined on the basis of the number of workers currently employed on each shift. On this basis 1,600 apartments were considered to be on the day shift, 1,600 on the night shift, and the remaining 800 on the "graveyard" shift. The load fluctuations during the day were estimated for each group separately. On this basis the coincidental demands at the time of the system peak were estimated to be 832 kw for the first shift, 521 kw for the second shift, and 209 kw for the third shift, making the total of 1,562 kw for the 4,000 apartments.

The basic estimate of 0.4 kw average maximum demand per unit was used. After analysis of the load fluctuation on the three-shift basis, the average was lowered to 0.3 kw per unit. Allowing for seasonal fluctuation and distribution losses, the resulting maximum demand per unit was calculated to be 0.39 kw per unit.

DORMITORIES

The dormitory units consist of ten dormitories, each housing approximately 400 men. A large complete electric kitchen furnishes meals at all hours for the men housed in them.

Each of the ten dormitory units has 83 kw connected in lighting and 17 kw connected in ventilating fans and boiler-room power. This makes a total connected load of 100 kw per dormitory. Each dormitory consists of 148 double rooms, 96 single rooms, and 4 lounges or club rooms.

An administration building has a connected load of 50 kw. The connected street lighting load is 50 kw.

The kitchen and mess halls have a total connected load of 267 kw. This consists of 180 kw in electric cooking and baking equipment; 59.8 kw in kitchen lighting, mess-hall lighting, and small kitchen motive power, and 27.2 kw in refrigeration, ventilation, and boiler-room motive power.

The load fluctuations during the day were estimated separately for each of the various load groups within each of the dormitory groups assigned to the three shifts. Similarly, the load for the kitchens and mess halls was estimated by analyzing the daily fluctuations for each load group separately.

The total connected load for the ten dormitories, administration building, street lighting, and the mess halls and kitchen was 1,367 kw. The annual maximum demand was estimated to be 725 kw.

TEMPORARY HOUSES

The temporary houses consist of 4,000 demountable units, each unit having the connected equipment and using the estimated energy per year shown in Table XVIII. The average monthly maximum demand per unit is estimated at 1.5 kw, based on test information which was secured from various sources.

The houses were segregated into groups applicable to each shift on the same basis as were the apartments. When the daily fluctuations of the combined groups on the three shifts were analyzed, the average monthly maximum demand at the point of use was estimated to be one kilowatt per unit. After accounting for seasonal variation and distribution losses, the annual maximum demand resulted in an average of 1.29 kw per unit.

PERMANENT HOUSES

The permanent houses consist of 700 units, each unit having the connected equipment and using the estimated amounts of energy per year shown in Table XIX. The average monthly maximum demand per unit is estimated to be 1.75 kw, based on test data.

The load on this class of houses was estimated on the assumption that most of them would be occupied by families of the permanent employees and not subject to shift variations. The annual maximum demand at the substation, including distribution losses, is estimated to be 2.29 kw per unit.

The demand requirements for both the permanent and temporary houses appear, perhaps, to be higher than ordinarily would be expected on an ordinary system. This is due to a realization of 100 per cent saturation of ranges, refrigerators, and water heaters. This equipment is all standard household size, the water heater having a 1,000-watt base element with a 1,500-watt booster, both thermostatically controlled.

Table XIX

Lighting	1.0 kw	600 kilowatt-hours
Small appliances	1.3 kw	
Refrigerator	0.2 kw	420 kilowatt-hours
Range	11.0 kw	1,380 kilowatt-hours
Water heater	2.5 kw	3,600 kilowatt-hours
Total	16.0 kw	6,000 kilowatt-hours

The use of this equipment where no individual house meter is provided places this service in the unmetered category. People living in these housing units will probably not be educated to unmetered use of electricity and will use much more than the general average. It is to be expected that on cool mornings many people will take advantage of the unmetered service and partially heat the house by means of using either the surface or oven elements.

Because of the three-shift working and living conditions, the load peaks may be of longer than usual duration. The morning cooking peak for those going to work on the day shift will probably be around 6:30 to 7:30 a.m. This peak will be followed by another one probably from 7:30 to 8:30 a.m. for those coming off shift at 8:00 a.m. Thus the peak period of the combined project will probably extend over a three- or four-hour period, during which there will not be a great variation between hourly peaks.

PUMPING AND COMMERCIAL

Water supply to the project is furnished by means of five 150-horsepower and one 30-horsepower pumps. At the time of the peak on the system it was considered that the demand would be 660 kw.

It was estimated that the commercial, industrial, and street-lighting requirements would approximate 2,000 kw of connected load. The maximum demand was estimated at 1,034 kw.

References

1. LIGHTING AND COOKING LOADS IN ARMY BARRACKS, **Harold M. Potts.** *Electric Light and Power*, December 1942, page 60.

2. ESTIMATED LOAD FOR 35,000-MAN ARMY CANTONMENT, **Harold M. Potts.** *Electric Light and Power*, January 1943, page 78.

TRANSACTIONS SECTION

Preprint of Corresponding Pages From the Current Annual AIEE Transactions Volume

Any discussion of these papers will appear in the June 1943 Supplement to Electrical Engineering—Transactions Section

The Rating of Power Transformers as Determined by the War Emergency

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Synopsis: Accurate rating of transformers is one of the most important elements in the problem of providing continuous service to the customers of the utility industry. Rather than attempt to follow a general rule for the loading of transformers it is believed that the urgency of wartime needs amply justifies the effort required to place individual ratings on each transformer bank of the utility under consideration. Experience has shown that such ratings vary widely. The transformers cover a wide range of sizes, designs, manufacturers, and periods of manufacture; with weights per kilovolt-ampere ranging from 7 to 20 pounds, excitation voltages ranging from 95 to 110 per cent, their daily load factors from 25 to 100 per cent, and their seasonal loading from 0 to 100 per cent. The consequent saving of "critical" material is self-evident when each case is considered individually.

During the war it is desirable to base station ratings on the thermal capacity of all the transformers normally available for service, rather than attempt to set up station ratings based on abnormally high emergency ratings of the remaining units after the assumed failure of one unit. It is essential that these higher station ratings be properly applied so that the danger of failure is minimized.

In order to evaluate this program of individual transformer ratings, only dry units are considered. Since moisture accelerates the rate of insulation deterioration[1] and lowers its dielectric strength, increased ratings are not assigned to a questionable transformer until its moisture content has been reduced to a satisfactory value. Once this factor is eliminated, consideration of the other factors can be taken with greater accuracy and safety. Insulation deterioration and oil sludging are affected both by the magnitude and the duration of their temperatures. However, because of the simplicity of the rating method, it is necessary to consider only the magnitude at first. But as the load increases, the duration as well must then be taken into account if a maximum utilization of transformer capacity is to be realized. The capacity of bushings, tap changers, and other current-carrying parts must be checked since they may prove to be the limiting factor.

Such a program of calculating transformer capacities based on load cycles, ambient temperatures, manufacturer's test data, and operating conditions is a large undertaking unless the work is systematized and carried only as far as load conditions warrant. Therefore, a system has been set up wherein the ratings are progressively increased as the load increases. The more detailed computations are needed only when the load approaches the ratings based on the more general and, consequently, more conservative method of rating. This procedure reduces the amount of calculating and load supervising to a minimum.

Method Used in Rating Transformers

This progressive method of rating transformers is divided into four steps:

1. The ambient temperature.
2. The manufacturer's test data and operating conditions.
3. The thermal capacity.
4. The length of time at each hot-spot temperature.

This last step which is called the equal-life method of rating takes the variations in daily peak loads, the variation in ambient temperatures, and the hot-spot temperature cycle into consideration. Each successive step which usually results in a higher rating utilizes the data obtained from the previous step.

Increased Ratings for Oil-Insulated Self-Cooling Units Due to Ambient Temperatures Below 30 Degrees Centigrade (First Step)

The first step in rating transformers is to take advantage of the ambient temperatures lower than 30 degrees centigrade. Ratings are assigned, based on the Pittsburgh Weather Bureau average monthly temperatures over a 64-year period (see Table I). In order to take care of the deviation from the average temperature, six degrees centigrade is added to the mean values to obtain acceptable values for rating purposes. The actual daily average temperatures exceed the accepted monthly values used for rating less than 15 per cent of the time. The American Standards Association rule[2] which states that the "capacity can be increased 1 per cent for each centigrade degree the average temperature is below 30 degrees centigrade" is then applied to obtain monthly ratings. It may be noted that in Table I, column 2, the accepted ambient temperature for July is 30 degrees centigrade and for December seven degrees centigrade, which results in a 100 per cent rating for the month of July and 123 per cent rating for December.

When the banks are located indoors, the assumed ambient temperatures are increased five degrees centigrade under good ventilating conditions and ten degrees centigrade for average conditions. If the differential between indoor and outdoor temperatures exceeds ten degrees centigrade by measurement, the ventilation is usually improved. If the difference is less than ten degrees centi-

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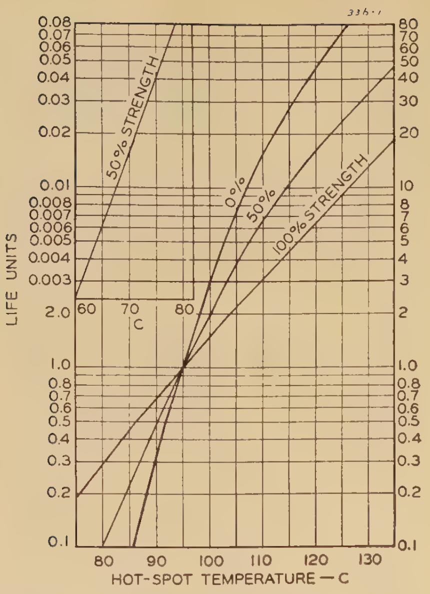


Figure 1. Life units versus hot-spot temperature for various degrees of insulation strength

One life unit equals one hour of operation at 95 degrees centigrade hot-spot temperature

grade, improvement is not usually justified.

Monthly ratings are of particular advantage in distribution substations where the lighting load is a considerable part of the total, since the shape of the seasonal load curve approximates that of the rating curve based on the outdoor ambient temperatures.

Increased Ratings of Oil-Insulated Self-Cooling Units Due to Design and Operating Conditions (Second Step)

When the load exceeds approximately 90 per cent of the rating as determined by the previous method, calculations based on the manufacturer's test data are made. In this, the losses in the transformer during the factory thermal test are calculated and compared with the actual losses in service of units built to the same specification. In determining the losses, corrections are made for the tap connections, operating temperatures, and applied voltages. In making these calculations the high- and low-voltage copper losses and the stray losses are considered separately, each being corrected to 95 degrees centigrade average copper temperature for the normal ratings. The core loss is corrected by some power of the ratio of the applied voltage to the rated voltage, the former voltage being determined from annual system voltage surveys. The exponent used is based on tests made at 100 per cent and 110 per cent excitation. If tests are not available at overexcitation, an exponent of 2.5 is suitable for most modern transformers. In the older designs this exponent ranges from 1.7 to 4.0. It must be remembered that this exponent is empirical and applies only to ratios near unity.

The temperature rises for the transformer under operating conditions at 100 per cent of rated name-plate current for the tap used are obtained by correcting the factory test values of both the hot spot over top oil and the top-oil rise by the ratio of the losses for the two conditions raised to the 0.8 power. The total losses are used for the top-oil temperature correction and the load losses for the hot-spot rise over top-oil correction. Curves of hot-spot rise over oil, hot-spot rise, and top-oil rise versus per cent load are then plotted, assuming that the rises vary as the 0.8 power of the losses as explained. (See appendix, Figure 5). This exponent is substantiated by information published in reference 3.

Before the ratings are assigned, it will be necessary to set up limiting temperatures for both the insulation and the oil. For normal ratings 105 degrees centigrade has been chosen as the limiting hot-spot temperature if the load factor is not too high. This agrees with the American Standards Association rating of 105 degrees centigrade for eight hours. The top-oil limits are set at 75 degrees centigrade for open or free-breathing transformers, 80 degrees centigrade for conservator-type units, and 85 degrees centigrade for inert gas units. These values are based mainly on experience, and the fact that sludging depends to some extent on the contact of the oil with oxygen as well as temperature and time. It is possible that these values may be revised upward if further experience justifies this action. The values chosen depend somewhat on the inspection period set up for oil reconditioning or filtering and the type of oil used.

Table I. Outdoor Ambient Temperatures

Used as a Basis for Rating Transformers Degrees Centigrade

Month	Average Daily Temperatures*	Average Daily Temperatures +6 C	Average Daily Maximum Temperature*	Average Daily Maximum Temperature +6 C
January	0	6	4	10
February	0	6	4	10
March	5	11	10	16
April	10	16	16	22
May	17	23	22	28
June	22	28	27	33
July	24	30	29	35
August	23	29	28	34
September	19	25	25	31
October	13	19	18	24
November	6	12	11	17
December	1	7	5	11

* Based on 64-year period from 1874 to 1938, Pittsburgh Weather Bureau.

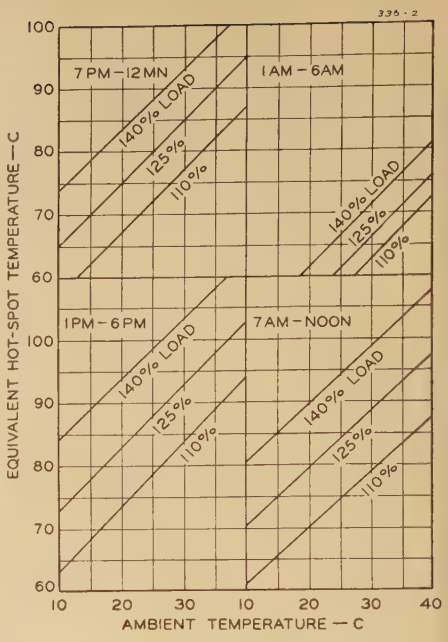


Figure 2. Illustrative equivalent hot-spot versus ambient-temperature curves for constant ambient temperatures and constant loads

Monthly ratings can now be assigned, based on the limiting hot-spot and top-oil temperatures, the ventilation, the outdoor temperatures, and the hot-spot and oil rise versus load curves. The ambient temperatures used for this method are the average daily maximum values plus six degrees centigrade as is indicated in column 4, Table I. It will be noted that the ratings are based on ultimate temperature rise and accepted maximum hourly temperature rather than the actual temperature rise and the daily average ambient temperature. This results in a somewhat conservative rating, which does not change with load cycle and requires little office supervision. Increased ratings of considerable magnitude are possible with this method of rating; yet periodic inspection of the load cycles and close scrutiny of the ambient temperatures are not necessary.

Increased Ratings Due to Thermal Capacity (Third Step)

When the load approaches the values assigned to the units by the preceding method, it is then necessary to take advantage of the thermal capacity of the units. Load cycles for a peak winter and a peak summer day are plotted and blocked. When transformer thermal transient calculations are being made, the shape of the load cycle curve at the time of the peak load is most important. More conservative ratings are obtained by averaging the peak load over a period of hours. For base load stations a four-hour average and for residential stations a two-hour average are about the shortest periods that should be used.

Any of the published methods of making thermal transient calculations may be used, but since curves have been prepared for the previous method of rating, advantage should be taken of this information. Load-cycle calculations for two or three assumed peak loads should be made for the winter and summer cycles, using the step-by-step method. The assumed peak loads should be of the order of magnitude expected for the monthly ratings; for example 110 per cent, 120 per cent, and 130 per cent may be assumed for the summer cycle if the ratings for these months are expected to be between these values. The curves of Figure 5, the transient-heating equation, the time-constant curve, and the various tables as explained in the appendix are of considerable assistance in making these computations. Monthly ratings are obtained from curves of peak hot-spot rise and peak oil rise plotted against per cent load for both the winter and summer cycles. The curves for the spring and fall months are obtained by assuming values halfway between the summer and winter curves. Various simplifications in the method of calculating become obvious as experience is obtained in making these computations. Hereafter these ratings which are used as a basis for normal everyday recurrent loading will be referred to as "recurrent" ratings.

These three methods of rating progressively applied result in considerably higher than name-plate ratings as is indicated by Table II, which gives the results for these three steps as well as the general method of the interim report[4] for a typical suburban distribution station. In this example the general method is the more conservative, but this is not always true.

Table II. Comparison of General and Individual Bases of Rating

Aliquippa Substation, Three-Phase, 3,000 Kva, 22.5/4.33 Kv, 400 Amperes

Location—Outdoors

Time Constant T—3.9 hours

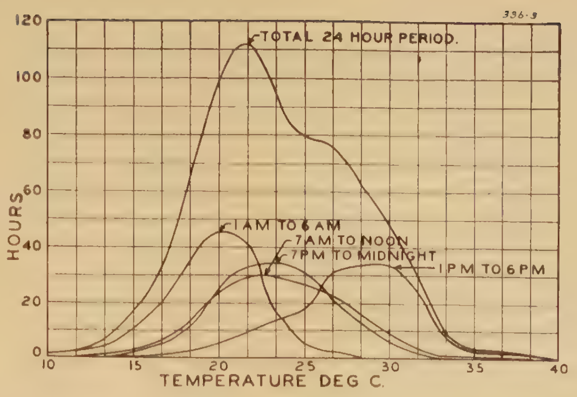
Method of Rating	Comparison of Rating (Per Cent) July	December
I. General (step 1—ambient-temperature basis)	100	123
II. Individual (step 2—test-data basis)	107	143
III. General (interim report, Figure 1)*	108	132
IV. Individual (step 3—load-cycle basis)	122	154
V. Individual (step 4—equal-life basis)	137	154**

* 70 per cent load factor in July and 64 per cent load factor in December.

** Limited by current-carrying parts.

Figure 3. July temperature-distribution curve for Pittsburgh and vicinity

The curves give the distribution for the total 24-hour period as well as for 6-hour periods



Industrial Versus Distribution Type of Substations

The same methods can be applied to rating both distribution and industrial substations. However, the monthly ratings have little meaning in industrial substations, as they usually have little or no seasonal trend, and in these cases the July rating would always be the limiting value. As the first step in rerating industrial substation transformers which have no seasonal trend, the load-factor method as given in the interim report which states "that the capacity may be increased 0.3 per cent for each one per cent the load factor is below 100 per cent" is quite satisfactory. However, it must be remembered that the magnitude of the hot-spot temperatures experienced at two stations having the same load factor but different load-cycle characteristics vary considerably. For example, a customer operating on one eight-hour shift may have the same load factor as one working three eight-hour shifts but would have considerably different hot-spot temperatures. More accurate methods for calculating the ratings are the individual methods discussed previously.

Increased Ratings Based on a Summation of the Life Consumed at Various Hot-Spot Temperatures (Fourth Step)

The fact that loads in most industrial plants show no seasonal trend suggests that one can afford to take more life out of the units during the summer months, as the insulation deterioration during the winter months may be almost negligible. This suggests a summation of the life consumed at each hot-spot temperature. This method of rating may be applied to either the distribution type or the industrial type of station, but the resulting rating increases are much greater in the latter stations. This method which has been called the equal-life method is now in the development stage and has not been proved by field experience.

In this method of rating, one hour of operation at 95 degrees centigrade is defined as one life unit. Assuming that the rate of transformer insulation deterioration varies in accordance with the data given in reference 1, Figures 6 and 7, a curve of hot-spot temperature versus life units can be plotted as shown in Figure 1.

The method of rating can be explained by giving a typical example. Three typical ambient temperatures such as 35, 25, and 15 degrees centigrade are added to the 24-hour hot-spot temperature-rise cycle for three different peak loads such as 110, 125, and 140 per cent and set down in table form. The three temperature-rise cycles are obtained from the previous method of rating (step 3). The hot-spot temperatures are then converted to life units by using Figure 1. The transformer assumed was considered to have deteriorated to 50 per cent of its original insulation strength. When the life units for the periods and constant ambient temperatures assumed are averaged, they can be reconverted into an equivalent continuous hot-spot temperature. The summary of this work for Aliquippa substation is shown in Table III, and the curves of equivalent hot-spot temperatures versus ambient temperatures for the three loads are given in Figure 2. The per cent load figure used in this method refers to the highest peak occurring during a 24-hour period. The day is divided into four periods, because in this type of station the heavy load occurs during the high-ambient-temperature period and the light load during the low-temperature period. A similar group of curves should be obtained for the Decem-

Table III. Reduction in Life Measured in Life Units for Aliquippa Substation Temperature Cycle (for Three Assumed Constant Ambient Temperatures)

Per Cent Load	110			125			140		
Ambient Temperature Period	15 C	25 C	35 C	15 C	25 C	35 C	15 C	25 C	35 C
Total Life Units Consumed During One 24-Hour Period									
1–6 a.m.	0	0	0.06	0	0.01	0.11	0	0.05	0.30
7 a.m.–noon	0.05	0.25	1.30	0.21	1.06	4.52	1.30	5.29	17.27
1–6 p.m.	0.08	0.48	2.37	0.42	2.10	8.72	2.47	9.95	31.50
7 p.m.–12 midnight	0.02	0.14	0.78	0.09	0.57	2.74	0.44	2.20	8.77
24-hour period	0.15	0.87	4.51	0.72	3.74	16.12	4.21	17.49	57.84
Average Life Units Per Hour for Above Cycle									
1–6 a.m.	0	0	0.010	0	0.002	0.018	0	0.008	0.050
7 a.m. noon	0.008	0.042	0.216	0.035	0.177	0.753	0.217	0.882	2.880
1–6 p.m.	0.013	0.080	0.395	0.070	0.350	1.460	0.418	1.660	5.250
7 p.m.–12 midnight	0.003	0.023	0.130	0.015	0.095	0.456	0.074	0.367	1.465
24-hour period	0.006	0.036	0.188	0.030	0.156	0.675	0.175	0.730	2.410
Equivalent Hot-Spot Temperatures* for Life Units in Above Table (Degrees Centigrade)									
1–6 a.m.	$t<60$	$t<60$	68	$t<60$	$t<60$	71	$t<60$	66	76
7 a.m.–noon	66	75	85	75	84	93	85	94	103
1–6 p.m.	69	79	89	78	88	98	89	99	108.5
7 p.m.–12 midnight	62	72	82	70	80	90	79	88	98
24-hour period	64	74	84	74	83	92	83	93	102

* Plotted in Figure 2.

ber temperature cycle if it differs to any great extent from the July cycle.

The next step is to obtain a curve showing the distribution of the outdoor ambient temperature versus hours for the months of July and December based on a number of years. Such curves for July based on the same four periods of the day are shown in Figure 3. A summation of the life units consumed during the month for the actual ambient temperatures and the three assumed peak loads can then be obtained by applying the July temperature-distribution values to Figure 2. This summation is given in Table IV. The summation of the life units divided by the number of hours in a month gives the average rate of consumption of life units and an equivalent hot-spot temperature for each per cent load.

A curve of per cent load versus equivalent monthly hot-spot temperature can then be plotted (Figure 4). Since the loads for the various days of the week usually vary, it is necessary to express the peak loads for each day in terms of name-plate rated current and convert to life units, make the summation, and reconvert to an equivalent monthly hot-spot temperature. This calculation is given in Table V. The limit for this method of rating is assumed to be the consumption of an average of 0.5 life units per hour which is equivalent to continuous operation at 90 degrees centigrade hot-spot temperature. The rate of deterioration is fairly high at this temperature, and so the limit may have to be reduced as experience in this method is obtained.

When the summer and winter load cycles differ, calculated values of equivalent monthly hot-spot temperatures for two months, July and December, usually suffice, as similar values for other months can be obtained by weighing the two calculated values, taking the accepted monthly ambient temperatures into consideration. From the monthly values the total life units expended annually and an equivalent annual hot-spot temperature can be obtained by the method given in Table V.

A word of caution should probably be added here. In order to take advantage of these increased ratings, it is essential that the transformer be in good condition, that is, the moisture content should be low, the current-carrying parts such as tap changers and bushings should have sufficient capacity, and the oil-expansion space should be ample. It is also necessary to check the oil temperatures to see that the rate of sludging is not too high.

Other Methods of Increasing Capacity

Rating transformers on an individual basis has an additional advantage in that it indicates what corrective measures are necessary to increase their capacity. For example, if oil temperature is the limiting factor, improved ventilation, the installation of additional radiators, or the installation of inert gas equipment which permits operation at higher oil temperatures will usually result in relatively large increases in capacity. If overexcitation is limiting the rating, installation of boosters in the neutral of star-connected transformers is the logical solution.

One result of the war has been the shift in the policy of rating two bank stations from the emergency rating of one transformer bank to the recurrent ratings of two banks. This has resulted in considerably increased station ratings, but it may necessitate load curtailment during some emergencies.

The rapid increase in the war industry and the difficulty in obtaining materials has necessitated the shifting of units from lightly loaded stations to release capacity for new war plants. Here again individually calculating bank capacities pay dividends, as banks having high time constants can be applied to the stations having peaked load cycles, and low time-constant banks can be applied to high load-factor stations where it is not possible to take advantage of the thermal capacity of the units. If the amount of inert gas equipment is limited, the ratings indicate the transformers on which this equipment can be used most profitably.

Auxiliary Cooling

When the other methods of increasing capacity have been exhausted, it is necessary to install some auxiliary method of cooling such as air blast, water-spray rings, or heat exchangers. The use of fans is usually the most convenient method, and, when sufficient fans are permanently installed, a 20 per cent increase in name-plate capacity is added to the recurrent rating without field tests. If more capacity is required, it is desirable to run tests on the units in the field. When tests are made, and the top-oil rise and gradients are established for the new conditions, the same three methods of rating as discussed previously can be applied. However, it must be remembered that the exponents used in this case will be 1.0 instead of 0.8. Additional capacity up to approximately 50 per cent is possible with this method of cooling.

Emergency Ratings

In spite of all precautions failures will occur. So emergency ratings still have an essential purpose in system operation, because they point out the bottlenecks in case of such failures. Wherever it is possible that emergency ratings may be exceeded, plans must be made to take care of the various contingencies that may arise.

In calculating emergency ratings, the usual method employed is that discussed under step 3; with higher temperature limits being assumed, 115 degrees centigrade is used for the hot-spot limitations and 90 to 95 degrees centigrade for the top-oil limitation, the calculations being made for a typical 24-hour load cycle at the station considered. This calculated rating based on average conditions is used mainly for planning. Other short-time emergency ratings with higher temperature limits are used for special conditions where the load can be relieved by switching or by the installation of fans. When an actual emergency occurs, the load-carrying ability of the units is calculated on the spot, based on the actual ambients, loads, and probable duration of the emergency. It is desirable that all essential information on the transformer affected at the time of the emergency be available so that every possible kilovolt-ampere of capacity can be utilized without undue damage to the units. Curves or charts showing the variation of top-oil temperature and hot-spot gradients with load, together with the actual top-oil temperature and hot-spot readings, are very helpful in determining the allowable overload. In an actual emergency, every effort is made to relieve the overload by transferring load or by the use of fans, although the emergency ratings are not necessarily being exceeded, as it is desirable to prevent abnormal rates of insulation deterioration.

In the majority of cases, hot-spot meters are not installed, so in these cases

Table IV. Aliquippa Substation—Summation of Life Units for the Month of July

		110 Per Cent Load			125 Per Cent Load			140 Per Cent Load		
Ambient Temperature	Hours	Equivalent Hot-Spot Temperature (Degrees)	Life Units Per Hour	Total Life Units	Equivalent Hot-Spot Temperature (Degrees)	Life Units Per Hour	Total Life Units	Equivalent Hot-Spot Temperature (Degrees)	Life Units Per Hour	Total Life Units
1–6 A.M.										
10	1.18	$t<60$			$t<60$					
11.7	2.44	$t<60$			$t<60$					
13.3	4.88	$t<60$			$t<60$					
15.0	10.75	$t<60$			$t<60$					
16.7	20.00	$t<60$			$t<60$					
18.3	35.50	$t<60$			$t<60$					
20.0	45.75	$t<60$			$t<60$			60	0.0025	0.12
21.7	41.00	$t<60$			$t<60$			62	0.0036	0.15
23.3	17.00	$t<60$			61.2	0.003	0.051	64	0.005	0.09
25.0	5.12	$t<60$			62.5	0.004	0.020	66	0.0074	0.04
26.7	2.13	60.3	0.003	0.006	63.8	0.005	0.011	67.5	0.01	0.02
28.3	0.25	62.0	0.004	0.001	65.0	0.006	0.002	69.0	0.013	
	186.0			.007			0.084			0.42
7 A.M.–Noon										
11.7	0.38	63.0	0.004	0.002	71.0	0.019	0.007	81	0.115	0.04
13.3	1.80	64.5	0.006	0.011	72.5	0.024	0.043	83	0.165	0.30
15.0	3.31	66.0	0.007	0.023	74.0	0.032	0.106	85	0.225	0.75
16.7	6.25	67.7	0.010	0.063	75.6	0.046	0.288	86	0.260	1.63
18.3	13.50	69.3	0.014	0.189	77.3	0.074	1.000	88	0.360	4.86
20.0	24.00	71.0	0.019	0.457	79.0	0.090	2.160	90	0.480	11.52
21.7	29.54	72.5	0.025	0.738	80.7	0.110	3.250	92	0.66	19.50
23.3	29.00	74.0	0.032	0.928	82.3	0.145	3.820	93	0.76	22.00
25.0	26.54	75.5	0.044	1.170	84.0	0.200	5.308	95	1.00	26.54
26.7	22.35	77.1	0.058	1.300	85.2	0.230	5.140	96	1.15	25.70
28.3	15.48	78.7	0.078	1.210	86.8	0.300	4.644	97	1.30	20.10
30.0	8.60	80.4	0.110	0.946	88.3	0.380	3.270	98	1.50	12.92
31.7	2.87	81.9	0.135	0.388	90.0	0.490	1.407	100	1.95	5.60
33.3	0.88	83.4	0.170	0.150	91.5	0.600	0.527	101	2.25	1.98
35.0	0.75	85.0	0.225	0.169	93.0	0.760	0.570	103	2.90	2.18
36.7	0.75	86.5	0.280	0.210	94.7	0.980	0.735	104	3.30	2.48
	186.0			7.954			32 275			158.10
1–6 P.M.										
15	0.38	69.0	0.013	0.005	78.0	0.068	0.026	89	0.46	0.17
16.7	1.13	70.7	0.018	0.020	79.7	0.096	0.109	91	0.56	0.63
18.3	3.00	72.3	0.023	0.069	81.3	0.125	0.375	93	0.76	2.28
20.0	5.38	74.0	0.032	0.170	83.0	0.165	0.888	94	0.95	5.06
21.7	9.50	75.7	0.046	0.437	84.7	0.210	2.000	96	1.15	10.94
23.3	13.38	77.3	0.060	0.803	86.3	0.270	3.615	97	1.30	17.40
25.0	17.82	79.0	0.082	1.462	88.0	0.360	6.43	98	1.50	26.80
26.7	31.25	80.7	0.115	3.600	89.7	0.460	14.38	100	1.95	61.00
28.3	33.50	82.3	0.145	4.85	91.3	0.600	20.10	101	2.25	75.50
30.0	33.50	84.0	0.195	6.53	93.0	0.730	24.50	103	2.90	97.30
31.7	23.50	85.7	0.290	6.83	94.7	0.950	22.35	105	3.70	87.00
33.3	7.13	87.3	0.330	2.37	96.3	1.20	8.56	107	3.80	34.20
35.0	2.53	89.0	0.420	1.06	98.0	1.50	3.80	108.5	5.60	14.10
36.7	2.25	90.7	0.540	1.22	99.7	1.90	4.28	110	6.45	14.50
38.3	1.75	92.3	0.700	1.23	101.3	2.35	4.12	112	7.70	13.48
	186.00			30.66			178 67			460 35
7 P.M.–Midnight										
15.0	2.75	62.0	0.004	0.011	70.0	0.016	0.044	79	0.082	0.23
16.7	5.13	63.3	0.005	0.026	71.7	0.022	0.113	80	0.10	0.51
18.3	11.88	65.3	0.007	0.083	73.3	0.028	0.332	82	0.14	1.66
20.0	24.63	67.0	0.009	0.222	75.0	0.039	0.962	83	0.165	4.07
21.7	32.60	68.3	0.012	0.382	76.7	0.052	1.695	85	0.225	7.35
23.3	34.50	70.3	0.017	0.586	78.3	0.072	2.880	87	0.320	11.08
25.0	30.25	72.0	0.023	0.685	80.0	0.100	3.025	88	0.360	10.88
26.7	21.25	73.3	0.029	0.616	81.7	0.130	2.765	89	0.420	8.93
28.3	12.13	75.3	0.042	0.552	83.3	0.170	2.233	91	0.520	6.83
30.0	6.38	77.0	0.058	0.370	85.0	0.225	1.422	93	0.760	4.85
31.7	1.94	78.3	0.074	0.144	86.7	0.300	0.582	94	0.870	1.69
33.3	1.00	80.3	0.110	0.110	88.3	0.380	0.380	96	1.15	1.15
35.0	0.56	82.0	0.140	0.079	90.0	0.480	0.269	98	1.50	0.84
	186.00			3.87			16.700			60.07
Grand total	744.00	77.0	0.057	42.49	87.0	0.306	227.73	94.5	0.915	679.47

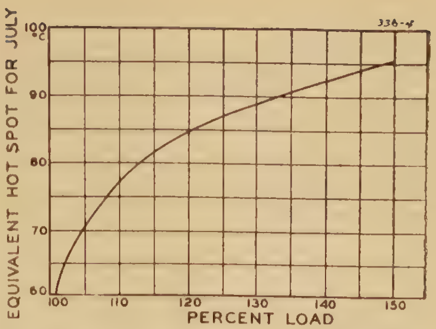


Figure 4. Illustrative curve for a distribution substation transformer showing the July equivalent hot-spot temperature versus per cent load

Table V. Aliquippa Substation Transformer Rating on an Equal-Life Basis for the Month of July

	Per Cent Load	Equivalent Hot-Spot Temperature	Life Units
5 weekday peaks*	137	91.5	0.61×120 hours = 73.20
Saturday peak	125	87.0	0.31×24 hours = 7.24
Sunday peak	100	60.0	0.0025×24 hours = 0.06
		90.0**	0.48×168 hours = 80.50

* All the weekday peak loads are the same magnitude and shape. The Sunday peak has a slightly different shape, but, because of its comparatively low magnitude, it has little effect on the results.

** The limit for this method of rating is assumed to be the utilization of an average of 0.5 life units per hour which is equivalent to continuous operation at 90 degrees centigrade hot-spot temperature.

it is necessary to add a calculated hot-spot rise to the top-oil temperature as read, to determine the transformer operating temperature. A running curve of the load, top-oil temperature, and hot-spot temperature plotted during the emergency is very helpful in determining when load curtailment is necessary. If service being fed by the stressed units is vital to the war effort, higher rates of depreciation would probably be accepted as long as a second failure is not imminent. Usually, however, when emergencies do occur, the ambient temperatures are lower, the loads are lighter, or the duration is shorter than the conditions assumed for calculating the ratings.

Conclusion

Individual transformer ratings for recurrent loads are amply justified by the additional capacity which is then known to be available for the war effort.

Some 20 customers have already been carried with loads up to 60 per cent above name-plate rating. AIEE limits of hot-spot operation have not been exceeded as yet.

So far there have been no failures of transformers due to loading nor any cases where it is believed that the rate of insulation deterioration is excessive.

Appendix

The following development is given to indicate a desirable form of the standard heating and cooling equations to use when a large number of calculations are to be made, using the step-by-step method of increasing ratings:

$$W = W_s + W_d$$

$$K\theta_u^{1.25} = Cp\theta + K\theta^{1.25}$$

$$p\theta_i = \frac{K(\theta_u^{1.25} - \theta_i^{1.25})}{C}$$

Initial slope of temperature heating curve

$$= p\theta_i = \frac{\theta_u - \theta_i}{T}$$

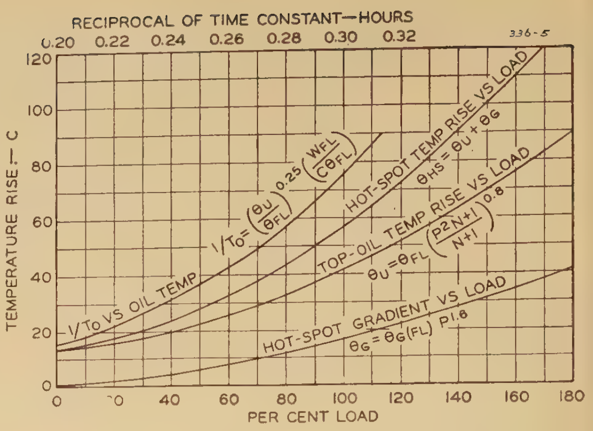
Therefore

$$\frac{1}{T} = \frac{K(\theta_u^{1.25} - \theta_i^{1.25})}{C(\theta_u - \theta_i)} = \frac{K\theta_u^{0.25}}{C} \frac{(1-R^{1.25})}{(1-R)}$$

$$\theta_u = \theta_{FL} \frac{(W_u)^{0.8}}{(W_{FL})^{0.8}} = \theta_{FL} \frac{(P^2N+1)^{0.8}}{(N+1)^{0.8}}$$

Figure 5. Illustrative curves for a typical transformer

These curves are used for rating transformers on an ultimate oil-temperature basis and for the thermal transient calculations



Therefore

$$\frac{1}{T} = \frac{K\theta_{FL}^{0.25}}{C} \frac{(\theta_{FL})}{(\theta_{FL})} \frac{(P^2N+1)^{0.2}}{(N+1)^{0.2}} \frac{(1-R^{1.25})}{(1-R)}$$

$$\frac{1}{T} = \underset{(1)}{\frac{(W_{FL})}{(C\theta_{FL})}} \quad \underset{(2)}{\frac{(P^2N+1)^{0.2}}{(N+1)^{0.2}}} \quad \underset{(3)}{\frac{(1-R^{1.25})}{(1-R)}}$$

$$\frac{1}{T} = \underset{(4)}{\frac{1}{T_o} \frac{(1-R^{1.25})}{(1-R)}}$$

1. The constant $(C\theta_{FL})/(W_{FL})$ is used as a time-constant reference figure for the transformers.

2. The term $(P^2N+1)/(N+1)$ for exponents of 0.2, 0.8, and 1.0 are set up in table form for values of N from 1 to 4 and P from 0.2 to 1.6 in 0.2 steps.

3. The term $(1-R^{1.25})/(1-R)$ can also be set up in curve or table form.

4. A curve of $1/T_o$ versus top-oil temperature is also plotted as shown in Figure 5.

5. A table of $(1-e^{-t/T})$ for various values of $-t/T$ is also of considerable assistance in solving the transient heating equation $(\theta_f - \theta_i) = (\theta_u - \theta_i)(1-e^{-t/T})$.

When one is solving the transient-heating equation for a 24-hour period, it is desirable to start the calculation at the end of the minimum load period. This is usually about six o'clock in the morning. The initial oil temperature (θ_i) which must be assumed for the first step, is usually about two or three degrees above the ultimate temperature rise for the minimum load. With a little experience, it is possible to estimate this temperature in advance within one degree centigrade. If the error in the original assumption is too great, the transient calculation should be continued until the maximum oil temperature is obtained. The values of (θ_u) and $(1/T_o)$ are obtained from Figure 5, the values of $(1/T)$ are obtained by multiplying $(1/T_o)$ by the function of R as explained above. The values of $(1/T_o)$ for the heating portion of the cycle corresponds to (θ_u) and for the cooling portion of the cycle to (θ_i).

List of Symbols

C = thermal capacity
N = ratio of copper to no-load loss
P = ratio of load amperes to name-plate rated amperes
$p = d/dt$
R = ratio of (θ_u/θ_i) for cooling cycle and (θ_i/θ_u) for heating cycle
t = time in hours
T = time constant
T_o = time constant for zero initial rise
W = watts loss in transformer
W_d = watts dissipation
W_s = watts stored
W_{FL} = watts loss at full load under operation conditions
W_u = watts loss at load which would produce θ_u
θ_{FL} = oil-temperature rise at full load under operating conditions
θ_i = initial oil-temperature rise
θ_u = ultimate oil-temperature rise
θ_f = final oil-temperature rise
θ_G = temperature gradient between hot-spot and top-oil temperature (ultimate values are used throughout)
θ_{HS} = hot-spot-temperature rise

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Protective Relaying for Long Transmission Lines

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UP to a certain length of transmission line, conventional distance relays will readily distinguish internal faults, for which the line should be opened, and external faults, load conditions, and power swings, for which the line should not be opened. This distinction is based on the magnitude of the impedance or reactance of the line as it appears to the relay. This impedance will be referred to hereafter as the input impedance and is determined by the current and voltage existing at the relay location.

The magnitude of the input impedance will distinguish clearly between internal and external faults for any length of lines, but, as longer lines are considered, the difference in impedance between load and fault conditions becomes less and may even be reversed. On the Kansas–Nebraska line, using conventional distance relays, there would be undesirable tripping on power swings from which the system can recover easily, such as those that may occur when the line is reclosed instantaneously after clearing a temporary fault. The power swing in this case is caused by the two terminals having drifted apart slightly during the reclosing period, because the sending end has lost load, causing it to speed up, and the receiving end has lost power, causing it to slow down; therefore, when reclosure occurs, a large amount of power flows and tends to draw the two ends together and thereby reduces the flow of power, and an oscillation is started.

Although there is little difference in the impedance magnitude during fault and load conditions on very long lines, there is, fortunately, a considerable difference in phase angle. During load conditions the heavy charging-current component tends to make the input impedance have a leading power-factor, whereas during an internal fault much of the line capacitance is short-circuited and the input impedance is therefore lagging considerably as shown in Figure 4 on curve Z_B.

As a result of the foregoing, impedance magnitude was once more used to distinguish between internal and external faults, but the distinction between faults and load conditions was based on phase angle, a quantity which was so selective that it was possible to differentiate further between load conditions, so that tripping was prevented on normal loads and power swings but allowed on out-of-step conditions.

No special relay construction was used. All the relay elements were of standard design with the reactance and resistance of their potential coil circuits arranged to obtain the desired characteristics. The equipment has certain advantages over the conventional distance-carrier-relay equipment and is not limited to extremely long lines; it can be used also for lines as short as about 50 miles. The equipment was developed for the 270-mile 154-kv interconnection between Nebraska Power Company and Kansas Gas and Electric Company systems to meet the general performance specifications laid down in a companion paper,[1] and it has been applied since to the "Ark–La" 154-kv line, approximately 200 miles long in Oklahoma and Arkansas.

Impedance Diagrams

If capacitance and shunt loads are neglected, the transmission of power over a simple single-line system can be represented vectorially as shown in Figure 2 wherein V_s and V_r represent the voltages behind the terminal generator impedances, and the voltages V_a, V_b, and so forth, are the voltages existing at various intermediate points. The system interchange current I_S and the angle δ will both increase in magnitude as the power transmitted is increased.

The relays located at the various intermediate substations will see impedance values determined by the relation between the substation voltage (such as V_a or V_b) and the current I_s. This value is the equivalent input impedance of the system and will be referred to as the input impedance. As the load changes, θ, V_a, V_b, I_s, and the input impedance will change. In a companion paper[1] the loci of the input impedance at a given location for various ratios of line-terminal voltages are shown to be circles on a resistance-reactance diagram. Furthermore, it is shown that, for the special ratio of line-terminal voltages $E_s/E_r=D$, where D is one of the "general line constants," the radius of such a circle is infinity, and the circle becomes a straight line.

For short lines where capacitance and shunt loads can be neglected the general line constant $D=1$, and thus, if the terminal voltages V_r and V_s are always equal, the loci of input impedance as "seen" from terminals R and S (Figure 2) are straight lines coinciding with the 0 per cent and 100 per cent lines of the distance scale on Figure 3. When these straight-line loci obtain simultaneously at the two terminals of a circuit, the loci of the impedances "seen" at intermediate points on the circuit are likewise straight lines and form a family of parallel lines as in Figure 3, which was first presented to the Institute in a discussion by J. H. Neher.[2]

Under the conditions for which it is valid, that is, systems with negligible capacitance, no shunt load, and equal terminal voltages, Figure 3 shows that the input impedance, as seen from any particular point, is a minimum when the

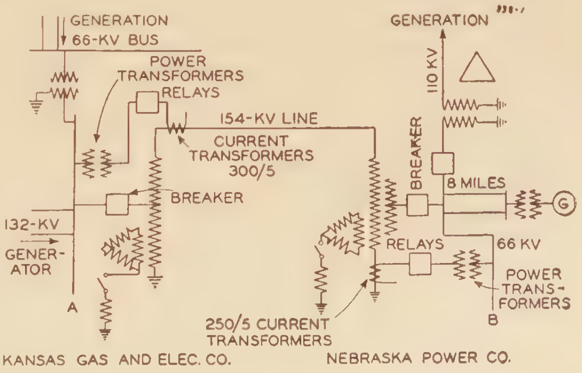

Figure 1. Connections of relays relative to transmission line

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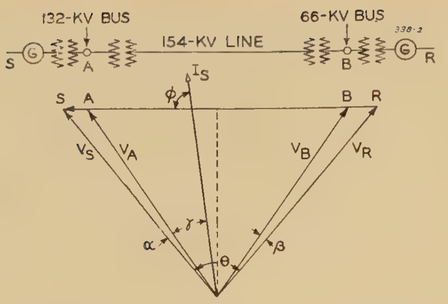


Figure 2 (left). Vector diagram of circuit in Figure 3

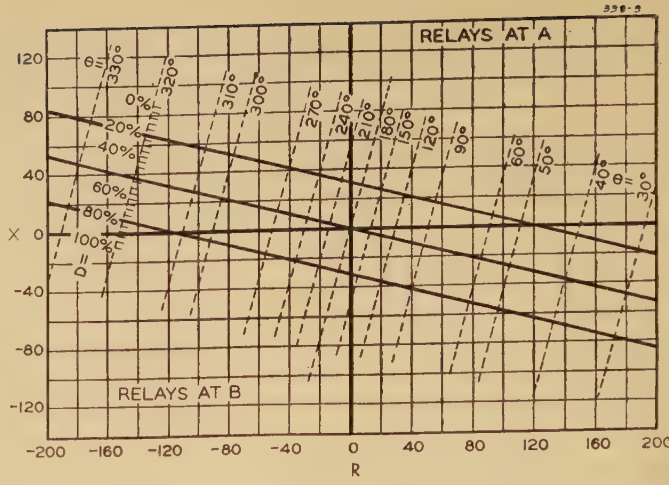


Figure 3 (right). Input-impedance diagram for relays at various locations along a line with $X/R = \tan 75°$ connecting two systems or machines, applicable to the special case in which $V_s = V_r$ assuming no shunt load and neglecting shunt capacitance

D = Distance of relay location from equivalent internal voltage of the leading end V_s, in per cent of total impedance from S to R

The co-ordinates are scaled in per cent of total impedance from S to R. Abscissas are resistance; ordinates are reactance

terminal voltages are displaced 180 degrees, and is infinite when the equal terminal voltages are in phase, since under this condition there is no current flow.

This type of chart is very convenient for determining whether or not a particular relay will operate for a particular amount of load transfer or for a particular angular displacement under swing conditions. To determine this, it is necessary only to plot the relay pickup characteristics on the diagram (being sure to plot both characteristics to the same scale) and observe at what value of the line-displacement angle θ the relay characteristic crosses the line characteristics.

In the more general case for which Figure 3 is not valid (that is, where terminal voltages are not equal, line capacitance is large, or there are important shunt loads), it is evident that the families of input-impedance circles applying to each relay location must be examined and compared with the relay pickup characteristics to determine if that relay is suitable for the proposed application.

In Figure 4, which represents a typical input-impedance circle for the 270-mile Kansas–Nebraska interconnection point *A* represents the system impedance for a fault at the far end of the line, and point *D* the impedance for a more remote fault up to which backup protection is desired. The third zone of the conventional impedance relay set to operate in backup time up to *D* would also operate on the maximum load of 50,000 kw. The conventional reactance relay with the same setting would not operate on this load, because it is outside the characteristic of the starting unit, but it could not be used as a basis for carrier relaying, because the impedance fault detector would experience the same trouble as the third step of the impedance relay and would transmit carrier and set up the out-of-step blocking circuit, and thereby delay tripping on faults.

A further margin of impedance would be required with either kind of relay to take care of the drifting apart of the two generating sources during the period of reclosure, because of the leading and losing load end speeding up and the lagging end losing generation and slowing down; otherwise, the relays would trip on the reduced impedance condition caused by the two stations swinging apart before reclosure.

The problem then was to design relays which would:

1. Trip instantaneously for faults up to *A*, including arc resistance.
2. Trip on out-of-step conditions and swings from which the system will not recover.
3. Not trip on loads up to the steady-state power limit.
4. Trip in delayed time up to *D*, for backup protection.
5. Not trip on swings from which the system would be likely to recover, that is, roughly up to $\delta = 110$ degrees.
6. Provide carrier blocking for external faults.

The Solution

Items 1, 2, and 3 require that the relays should trip instantaneously for conditions within the area *A*-*A′* 120 degrees–*B*-*B′* 240 degrees. A circle fits very well around these points (see Figure 4), the center of the circle being on the line-impedance line *AB*, about 60 degrees lagging the *R* axis, though it also includes some loads which should be excluded under item 3. Such a circle is the characteristic of a directional relay with a voltage restraint, which is similar to the starting unit of the standard reactance-type distance relay. This unit measures a quantity of the form $I \cdot f(\phi)/V = Y \cdot f(\phi)$ which is "admittance at an angle." Since this quantity is in mhos, the unit is called a "mho unit," corresponding to the term "ohm unit" applied to the reactance-measuring unit of the distance relay, which measures "impedance at an angle."

Items 4, 5, and 6 require a relay combination which will operate within the shaded area of Figure 5. The straight sides of the area are similar to the characteristics of a reactance unit except that they represent constant impedance at 20 and 30 degrees leading the unity-power-factor position instead of lagging it by 90 degrees. The ends of the area were cut off by the circular characteristic of an impedance unit as shown in Figure 5. In other words, by connecting *a* contacts of the impedance unit and the two ohm units in series with a timing unit, backup protection with delayed tripping occurs in the lightly shaded area *KPQ*. By connecting a set of *b* contacts of the three units in parallel in the carrier-control circuit, carrier blocking is provided over the same area *KPQ*, which disposes of items 4 and 6.

In reference to items 2 and 5, by connecting the contacts of the ohm units in series with those of the mho unit, the value of δ at which tripping occurs can be controlled accurately by the settings of the ohm units; otherwise, this limit is determined by the mho unit, whose setting is based on the length of the line and hence cannot be adjusted to a value of δ also. With the present line, Figure 5 shows that the mho unit would prevent tripping on swings up to about $\delta = 75$ degrees, but it was decided that about 100 degrees would be required to provide sufficient margin for power swings, especially after instantaneous reclosure of the breakers following a fault.

Relay Settings

At this point it will be apparent why 20- and 30-degree characteristics were

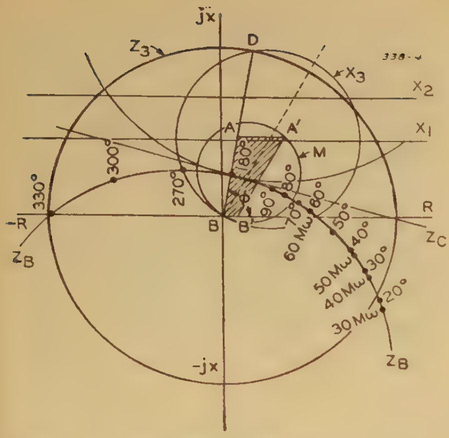


Figure 4. Impedance diagram of load and fault conditions on long line at terminal *B* of Figure 1

Angles marked on Z_B circle are angles between terminal voltages

AB = impedance of line to three-phase fault at end of line = 370 ohms

AA' = arc resistance with single-end feed, 60 miles per hour wind and minimum generation—fault at A—125 ohms

BB' = ditto with fault at B, near bus—32 ohms

AD = reach of backup (zone 3) protection

Mw = megawatts sent from A to B

chosen for the ohm units. As the characteristics were pulled in closer to the origin in order to prevent tripping on swings up to $\delta=110$ degrees, they had to be made parallel to the boundaries of the tripping area in order to insure tripping for all fault conditions.

The 20-degree characteristic can be as close to the line-impedance vector as desired but, the closer it is, the lower the ohmic pickup, and hence the higher the potential burden. A pickup of 42 ohms was chosen first for this unit but was increased later to provide higher relay torques.

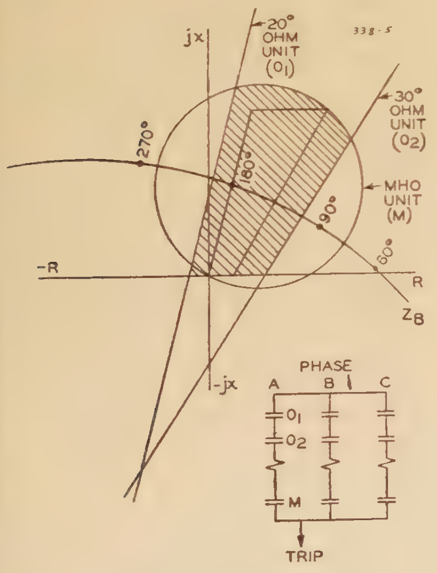


Figure 5. Tripping characteristic on phase faults and out-of-step conditions

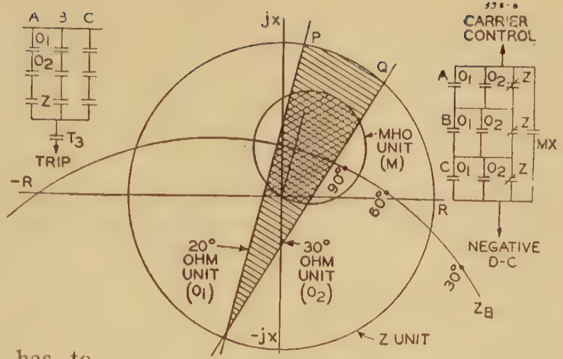


Figure 6. Blocking and backup protection on phase faults and power swings

Carrier blocking and time backup are provided in the shaded areas. Carrier is shut off by the mho unit in the double shaded area allowing instantaneous tripping for internal faults

The 30-degree characteristic has to allow for the maximum arc voltage, which will be during minimum generation and single-end feed.

Some approximate calculations of arc resistance for this circuit indicated that it might be between 50 and 70 ohms for a nearby fault and between 100 and 150 ohms for a fault at the far end. To allow for these arc values and to prevent tripping on swings required a setting of 72 ohms.

The diameter of the mho-unit circle represents its setting in ohms. To cover the fault area AA' BB' it was made initially about 375 ohms but was increased later to provide greater pickup torque and consequently faster action on far-end faults. The setting of the impedance unit was merely a rough margin between nearby and remote faults, so as to prevent the carrier and the timing unit from being operated unnecessarily; the value chosen was 500 ohms.

The effect of combining these circuits and their characteristics is shown in Figure 8, and explained in the next few paragraphs.

Over-All Operation of Protective Relays

Figure 8 shows the various zones of operation, and Figure 9 is a schematic diagram of the d-c connections.

A phase fault on the 154-kv line is within both the carrier and tripping zones (see Figure 8 zone T_1). The ohm units O_1 and O_2 and the Z unit in the same phase open their *b* contacts in the carrier-control circuit, turning on carrier. The mho units M pick up the MX auxiliary relay which by-passes the contacts of the O and Z units and restores negative direct current to the grid of the oscillator tube in the transmitter stopping carrier. Another contact of the MX relay de-energizes the receiver-relay holding coil, and, with carrier off, the receiver relay then closes its contacts and causes tripping in a total time of about $1^{1}/_{2}$ cycles.

A ground fault picks up the overcurrent fault detector G whose *b* contact removes negative direct current from the transmitter grid while the current polarized directional relay GD reapplies negative direct current and stops the carrier. Other contacts of GD de-energize the receiver-relay holding coil and complete the tripping circuit through the contacts of the receiver relay.

An external fault within the range of the mho unit will not trip instantaneously through the receiver relay, because, at the faulted end, the mho unit is reversed and hence does not shut off carrier, so that instantaneous tripping is prevented, but the relay will trip in intermediate time (T_2), if the fault is not cleared by the correct breaker.

An external phase fault within the range of the ohm and impedance units turns on carrier as before and starts the timing unit (see zone T_3C of Figure 8), but at the faulted end the mho unit is reversed and hence does not shut off carrier, so that instantaneous tripping is prevented, but backup time (T_3) tripping will occur if the correct breaker does not trip. If the carrier is off for maintenance, the receiver-relay contacts R are short-circuited by the switch SC, so that tripping occurs directly when the mho unit closes (zones T_1 and T_2). Instantaneous tripping with "carrier off" is limited to the line and a little beyond the windings of the transformers at the far end by the setting of the mho units. If the fault is beyond this zone, the mho unit does not operate, and the timing unit operates in backup time (T_3), if the fault has not already been cleared. A fault within the reach of the mho unit but beyond the location of the opposite mho units will be cleared in intermediate time (T_2) if not cleared already by carrier relays.

An external ground fault within the range of the overcurrent blocking unit (G_1) turns on carrier, but at one end the directional unit (GD) is reversed and does not turn off carrier, so that instantaneous tripping is prevented. In that case, the time overcurrent ground relay (type IAC) provides backup protection, unless the proper differential relay has already operated.

Receiver Blocking Relay, RB

Carrier is started for all fault conditions, though it may be stopped almost at once by the directional relay, if the fault is in the tripping direction. If tripping occurs, the carrier will not be maintained for more than the time taken for the directional relay and its auxiliary relay to operate, which will be one to three cycles. The persistence of carrier for more than three cycles would indicate an external fault and would operate the *RB* relay which is arranged to pick up in three to four cycles and drop out in six cycles.

The *RB* relay energizes the holding coil of the receiver relay, so that, during external faults, the *RB* relay maintains the blocking action for another six cycles after the carrier has been shut off by the resetting of the relays after the fault has been cleared. This arrangement prevents tripping through the carrier relays unless the condition is maintained, as would be the case if a second fault occurred just after the first one had been cleared.

Tripping on Out-of-Step Conditions

The conventional relay equipment for short lines employs blocking relays which prevent tripping on both power swing and out-of-step conditions. These conditions are recognized by means of an impedance unit which applies blocking after a time delay of about 0.1 second, unless it is prevented by an instantaneous impedance unit with a lower ohmic setting; in other words, a swing is distinguished from a fault by the rate of change of impedance. To effect tripping during actual out-of-step conditions requires a somewhat expensive relay which requires several power oscillations before it can be sure of the out-of-step condition.

With the present equipment not only are the foregoing features inherent in the relay characteristics, but they trip instantly when the angle crosses a threshold value. Not only is no time required for the decision, but the angle itself can be set accurately from system impedance curves. In the present case a setting of 72 ohms prevents tripping for power swings up to $\delta = 110$ degrees.

The Mho Unit

The mho unit is a four-pole induction-cylinder unit, see Figure 7a; the two long poles have a potential winding whose flux induces eddy currents in the induction cylinder or rotor which react with the fluxes from the two short poles to produce torque. One of the short poles has two current windings excited by the currents in two of the phases, so as to make a flux proportional to their delta sum. The other short pole has a potential winding excited by the potential between the same two phases. The polarizing potential winding has voltage from the next lagging pair of phases. As an example, phase 1–2 relay has a torque proportional to: $V_{23}[(I_1-I_2)\cos(\sigma-\phi-30\text{ degrees}) - V_{12}\sin\sigma]$, where σ is the angle between V_{12} and V_{23}, so that it operates when $(I_1-I_2)\cos(\beta-\phi-30\text{ degrees}) > V_{12}\sin\sigma$, or when the admittance of the circuit

$$\frac{(I_1-I_2)\cos(\sigma-\phi-30\text{ degrees})}{V_{12}\sin\sigma} > Y\text{ mhos.}$$

The value of Y is adjusted by an auxiliary potential transformer with nine ten per cent and ten one per cent taps, so that one per cent steps can be obtained.

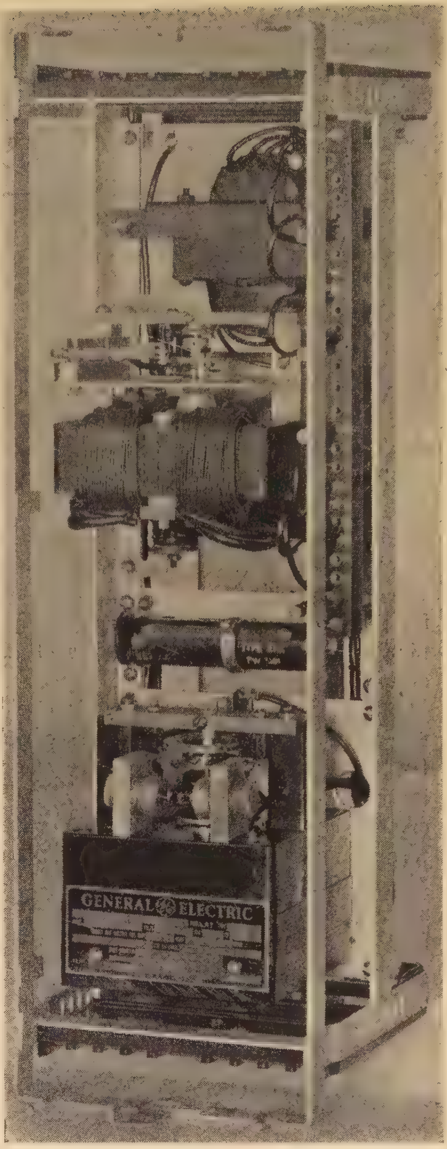


Figure 7a. Type CGZ special distance relay

The upper is the mho unit
The lower is the Z unit

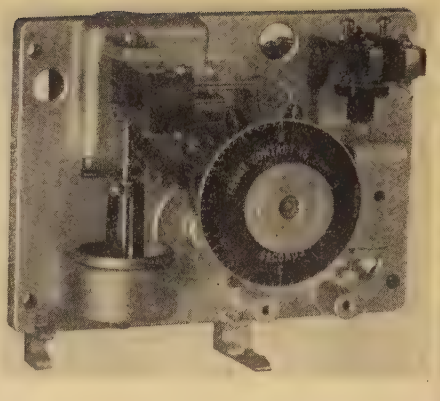


Figure 7b. Type *PDM* d-c timing unit (T_2 and T_3)

Figure 7c (right). Schematic connections of mho unit in phases 1–2

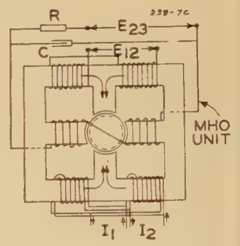
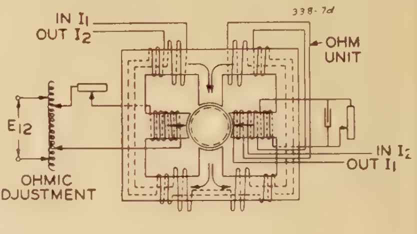


Figure 7d (below). Schematic connections of ohm unit in phases 1–2



Apart from its suitability for this application, the mho unit has the great advantage over the ohm unit of the conventional distance relay of not requiring a directional unit, since it is itself directional. On long lines, where arc resistance is not a large percentage of the total impedance, its accuracy for distance measurement is as good as that of a reactance or an impedance unit, and it is less likely to operate on loads and swings than either unit.

On the Kansas–Nebraska line $\phi = 72$ degrees for faults on the line, and for a fault at the end of the line there would be very little collapse of the voltage triangle, because most of the impedance is in the line, so that σ is about 115 degrees. Consequently, the ratio of the ohmic reach of the mho unit on three-phase faults to its reach on phase-to-phase faults is

$$\frac{\text{Three-phase reach}}{\text{Phase-to-phase reach}} = \frac{\sin 120^\circ \cos(115^\circ - 72^\circ - 30^\circ)}{\sin 115^\circ \cos(120^\circ - 72^\circ - 30^\circ)} = \frac{0.866 \times 0.974}{0.906 \times 0.951} = 0.98$$

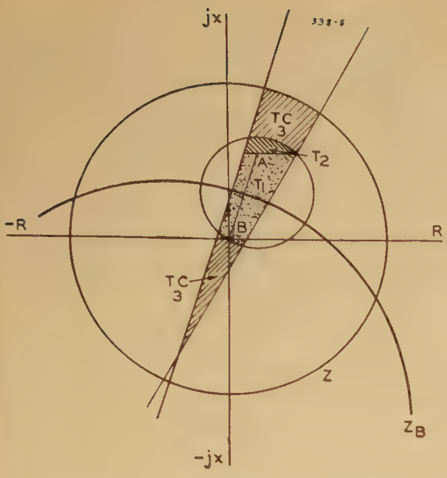


Figure 8. Over-all relay characteristics

In other words, the reach on three-phase faults is approximately the same as on phase-to-phase faults.

In order to provide fast operation on three-phase faults close to the station, the polarizing potential coil of the mho unit is made resonant, so that, when the line potential is suddenly reduced to substantially zero by the fault, the potential across the relay winding is maintained long enough to insure reliable operation. The connections of this circuit are shown in Figure 7c and are the same as are used in the standard *GCX* distance relay.

The Impedance Unit

The impedance unit is of the eight-pole induction-cylinder design (see Figure 7a), three of the poles producing current-operating torque and three producing potential restraining torque.

The windings on the three current poles are in series, but the middle pole has a copper sleeve, so that its flux is out of phase with that of the other two poles and hence produces torque. A similar method is used in the potential circuit where the middle pole is dephased by a series resistor. In the appendix, it is shown that this arrangement produces a steady torque, irrespective of the angle between the pole fluxes.

The induction-cylinder construction not only provides a high-torque low-inertia device, but it also has a pickup and dropout which are very close together; this is advantageous in a fault detector for very long lines where there is little distinction between the system impedance during load, swing, and fault conditions.

Its operating torque is proportional to the current squared, and its restraining torque is proportional to the adjacent voltage squared, so that the relay operates when the system impedance falls below its calibration. The pickup is independent of phase angle within four per cent, because of the steady torque of the induction cylinder.

Since the relay has only one current circuit, the reach on three-phase faults is 15 per cent more than on phase-to-phase faults, but this is of no consequence, because this unit controls the operation of carrier and time backup protection and hence need not be accurate.

The Ohm Unit

The ohm units are the same as that of the standard reactance-type distance relay, see Figure 7a, except that one is arranged to measure the 20-degree and the other the 30-degree component of impedance, instead of both measuring the reactive component. The change from the standard ohm unit was accomplished simply by changing the constants of the potential circuit to obtain the desired phase angle.

Each unit has four double-current windings arranged as in Figure 7d, so as to create a magnetic flux between the front and back poles of the unit. The left-hand pole has a potential winding whose phase angle is adjustable by a series resistor between 70 degrees and 55 degrees, so that the relay characteristic can be adjusted for maximum torque between 20 and 35 degrees. The right-hand pole has another double-current winding which is shifted 90 degrees in phase from the first by a floating secondary winding having a parallel capacitor and resistor.

The current flux from the front and back poles reacts with the flux from the right-hand pole to create an operating torque $K(I_1-I_2)^2$ where I_1 and I_2 are the currents in the double-current windings of the relay associated with phases 1 and 2. It also reacts with the flux from the potential pole to produce a restraining torque $E_R(I_1-I_2)\cos(\phi-\Delta)$ where Δ is the angle for maximum *EI* torque.

At the balance point the two torques will be equal so that $E_{12}\cos(\phi-\Delta)=K(I_1-I_2)$, but $E_{12}=E_1-E_2=(I_1-I_2)Z$, where Z is the impedance of a line conductor between the relay and the fault, therefore, $Z_1\cos(\phi-\Delta)=K$. K is adjustable by means of a transformer with one per cent taps.

The two ohm units in each phase have their contacts in series and are polarized in opposite directions (the 20-degree unit operates on incoming K_W and the 30-degree unit on outgoing K_W) so that the system impedance has to be between their characteristics in order to turn on carrier or to trip (see Figure 6).

Timing Unit

The timing unit is controlled by the same relays that control the carrier and is of the same design as used in the standard distance relay. It provides a short time delay for faults in the protected zone in case the carrier tripping is inoperative and a longer time delay for faults in the low-voltage windings of the transformer, and, during minimum generation at the other station, the third zone may reach a short distance into the system beyond the power transformer at the other station.

The speed of the timing unit is controlled by a centrifugal friction governor (see Figure 7b). A d-c solenoid winds up the spring which operates this unit. Two adjustable insulated contact arms close the intermediate and backup time contacts. The timing scale is graduated in tenths of a second up to three seconds. A time setting is made easily by loosening a knurled nut on the main shaft and setting the contact arms.

Ground Relays

The relays and connections for ground faults is the same as in the standard carrier-pilot relay scheme, except that the overcurrent tripping relay was made

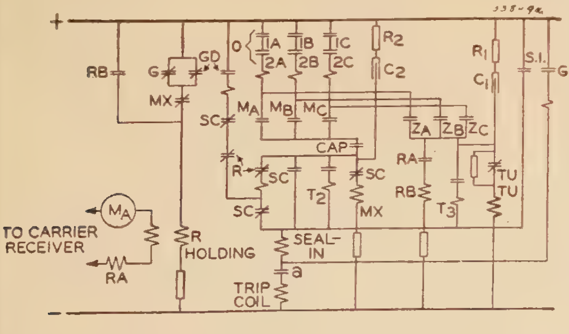


Figure 9. Circuits of carrier relays for long transmission lines

Figure 9a (left). D-c and tripping circuits

SC switch contacts shown in position for carrier on

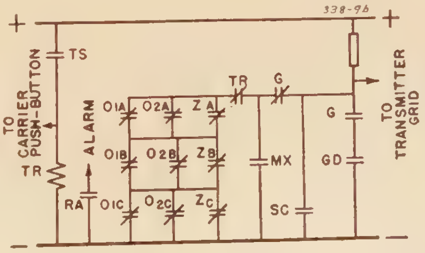


Figure 9b. Carrier control circuits

directional, thus eliminating an extra directional relay. The blocking unit is a plunger overcurrent unit which operates in a fraction of a cycle to remove negative bias from the grid of the oscillator tube in the carrier transmitter (see Figure 9b) and thus starts carrier. The tripping unit is an eight-pole induction-cylinder unit with alternate poles provided with a copper sleeve to lag their flux, so that a torque can be produced. An induction unit is used for the tripping function, because it is least liable to trip undesirably on transient conditions.

Transformer Differential Relays

These are the standard high-speed type *HDD* relays with harmonic restraint which were described in a previous paper.[3] Because of the absence of breakers on the line side of the transformers, it was necessary to provide a means of tripping the breaker at the other end for a transformer fault. This was done by arranging the differential relays to close a three-phase grounding switch on the 154-kv line which creates a fault and causes tripping at the other end.

Reclosing Relays

After the breakers at each end of the line have been tripped, they can be reclosed in four different ways:

1. Manually using the synchroscope.
2. Manually by automatic synchronizing.
3. Automatically with instantaneous or delayed reclosing.
4. Automatically with automatic synchronizing.

Figure 10 shows a detail of the reclosing circuit. Manual closing is done by closing the manual control switch (*CSW*) and the synchronizing switch on the right of Figure 10. Automatic synchronizing is initiated by turning switch 43 to position *S;* then the contacts 25 of the automatic synchronizing relay (type *GES*) will energize the reclosing circuit, provided that the voltage check relay 83 has operated, closing contacts 83*X* of its auxiliary relay.

In normal automatic operation it is customary to set the switch 43 at the position marked *R* and *S* at each end of the line. This provides one immediate reclosure at both ends of the line through the instantaneous step of the type *AC*-1 reclosing relay; this reclosure is made as fast as possible by connecting the initiating relay 79*X* in parallel with the trip coil, so that the timer is started at once, and the closing coil will be energized as soon as the breaker *b* switch is closed. If the fault persists and one end recloses first and energizes the line, the voltage-checking relay 83 at the other end operates; this opens contact 7–8 of 83*X* and transfers the control to the automatic synchronizing relay 25. Similarly, if only one end trips, the other end can only reclose through the synchronizing relay 25.

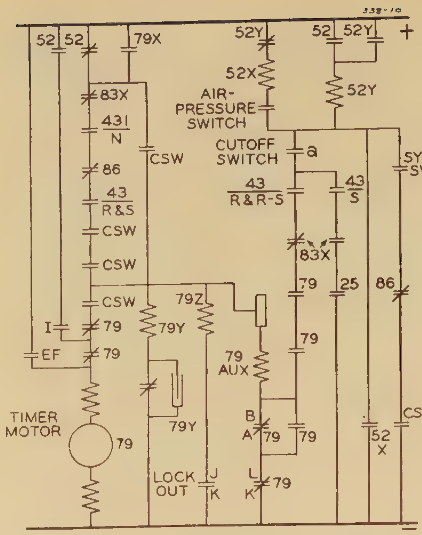


Figure 10. D-c connections of automatic reclosing circuit

A number of combinations are possible: for instance, one end may have the selector-switch 43 set to *R* and the other to *S*, so that one end recloses at once and the other through the synchronizer, and, of course, the two may be interchanged. If both ends are at *R* and have similar *AC*-1 time settings, immediate reclosure will occur at both ends three times unless the breaker stays in after the first or second reclosure; with different timer settings only the early end will reclose immediately. Other combinations are *R* with *R* and *S*, *S* with *R* and *S*, and *S* with *S*. Three reclosures are provided by the type *AC*-1 reclosing relay at adjustable intervals, the maximum setting being three seconds; after the third reclosure the reclosing circuit is locked out.

The type *GES* automatic synchronizing relay 25 gives the closing indication to the circuit breaker slightly in advance of synchronism, in order to effect closure of the breaker contacts at the moment of synchronism, regardless of the amount of frequency difference, unless it is excessive, in which case it will not give the closing indication. The angle of advance at which the relay functions is proportional to the slip frequency. The relay was described in detail in a previous paper.[4]

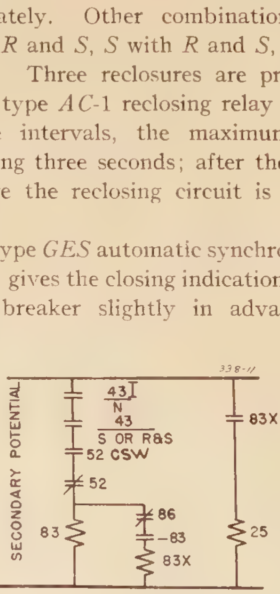


Figure 11. Connections of voltage and synchronism check relays

Reactor Control

In order to control voltage on the 154-kv line three 5,000-kva three-phase shunt reactors were provided at each end of the line which are connected automatically to the power transformer tertiary winding whenever overvoltage occurs or when the terminal breaker is opened. A short time delay is provided to avoid switching in the reactors during transient voltage disturbances resulting from line switching or from faults on the line.

Phase Faults During Heavy Load Conditions

Before the relay equipment was shipped, it was tested thoroughly on an artificial transmission line in the factory. These tests revealed that the directional properties of the mho units could not be relied upon above a certain value of load, that is, above a certain value of θ, the angle between the electromotive forces at the two ends of the line. This is caused by the fact that the voltage at the far end of the line controls the phase angle of the fault current, while the phase angle of the relay characteristic is controlled by the voltage at the near end. The fault current may therefore be far from the maximum torque angle of the relay and can be shifted still farther by the collapse of the voltage triangle on a nearby fault until it is near the zero torque position. Finally the superposition of the load or swing current actually may cause reverse torque, so that the mho unit may trip for a fault on the other side of the local transformer.

The likelihood of wrong tripping increases with the angle θ between the voltages at the ends of the system. With a bus fault the restraining torque of the mho unit is zero, so that it acts as a directional relay, and the conditions for wrong tripping can be expressed in terms of the components of current in the direction of maximum torque of the relay.

These expressions are derived in the appendix; they show that $\theta = 48$ degrees is approximately the maximum separation that can exist without causing wrong operation of the mho unit on incoming load. The phase 1–2 relay goes wrong

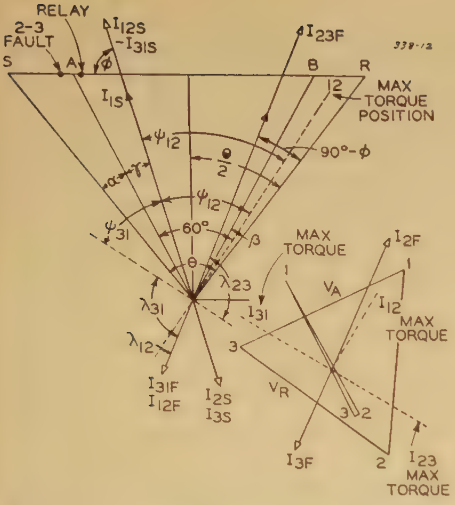


Figure 12. Vector diagram of bus fault with outgoing power

for a phase 2–3 fault; the other relays require more separation to make them go wrong.

Since power transfers in excess of this are expected, it was necessary to seek a solution. The solution to this difficulty was the addition of a polyphase directional-relay type *CAP15A* with its contacts arranged to supervise those of the mho unit. This relay has a torque proportional to the arithmetical sum of the 70-degree lagging components of the positive-sequence and negative-sequence volt-amperes and was described in a previous paper.[5]

The *CAP* relay operates correctly under these conditions of a bus fault for load conditions causing up to $\theta = 85$ degrees. The maximum loads expected would give $\theta = 53$ degrees, so that an adequate margin is provided. Under power-swing conditions it is, of course, possible to have more than 85 degrees displacement, but such a separation could only be reached during the reclosing period and would require a permanent fault; otherwise the tripping condition could not occur. While it was found possible to take care even of this condition, by means of a negative-sequence directional relay with a closing mechanical bias to permit operation on three-phase faults, yet it was regarded as a very improbable coincidence, and it was decided that even the *CAP* relay might never be called upon to prevent wrong operation.

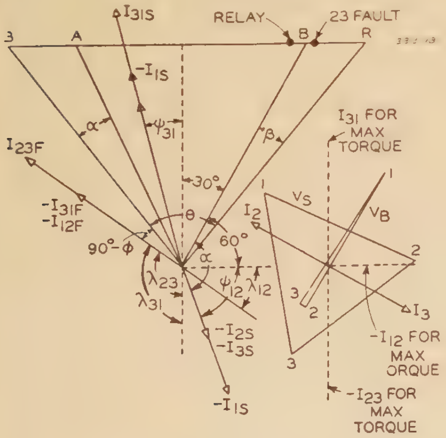


Figure 13. Vector diagram of bus fault with incoming power

System Tests

The calculated settings of the relays were checked very closely by their behavior during the system tests which are described in a companion paper. Internal and external faults were applied with zero power exchange and with the interconnection loaded approximately to its stability limit, all relay operations were correct.

Conclusions

The magnitude of the input impedance of a transmission line is always a reliable basis for locating faults, but, as the length of the line increases, impedance magnitude becomes less reliable for distinguishing between load and fault conditions. Fortunately, however, there is at the same time an increasing difference in phase angle.

Distinction by angle has the advantage that the degree of system separation for tripping can be controlled accurately and can be effected without delay.

Symbols

V_s = generator electromotive force at sending end
V_r = generator electromotive force at receiving end
V_a, V_b = terminal voltages at A and B
A, B = relay locations on transmission line
δ = angle between terminal voltages V_a and V_b
θ = angle between generator electromotive forces, V_s and V_r
α = angle between V_s and V_a
β = angle between V_r and V_b
σ = angle between polarizing and restraining potentials on the mho unit
ψ = angle between V_a or V_b and load component of current
λ = angle between V_a or V_b and fault component of current
ϕ = power-factor angle of input impedance looking into line $= \tan^{-1}\frac{X}{R}$

The following symbols are used on the diagrams:

M—Mho unit (phase directional unit)
O—Ohm unit

Figure 14. Relay panel

Z—Impedance unit
R—Receiver relay
TU—Timing unit
MX—Mho-unit auxiliary relay
RA—Receiver auxiliary relay
RB—Out-of-step blocking relay
G—Ground-fault detector
GD—Ground directional relay
GB—Ground backup relays
SI—Seal-in relay
TR—Carrier-test relay
TS—Carrier-test switch
SC—Carrier on-off switch
T—Target

A, *B*, *C* refer to the phases, for example, Z_A, Z_B, Z_C

1, 2, 3 refer to the zone of time or impedance, for example, O_1, O_2, O_3, T_1, T_2, T_3

25—Synchronizing relay
43—Transfer switch
43*I*—Inspection transfer switch
52—Circuit breaker
79—Reclosing relay
83—Voltage checking relay

86—Lockout relay

X, *Y*, *Z*—Auxiliary relays, such as 83*X* or 79*Y*

Appendix

The mho unit has zero voltage restraint during a bus fault involving more than one phase and therefore acts as a directional relay under that condition. The torque due to the swing current is $I_S \cos \psi$ where ψ is the angle measured from I_S to the maximum torque position. The torque due to the fault current is $I_F \cos \lambda$ where λ is the angle measured from I_F to the maximum torque position.

The following is an approximate analysis intended more as an explanation than as a rigid proof. The rigid analysis is extremely long and involved.

Referring to Figure 2, and assuming $V_S = V_R = V$ and that a 2–3 fault causes no drop in V_1

$$V_A \cos\left(\frac{\theta}{2} - \alpha\right) = V \cos\frac{\delta}{2} = V_B \cos\left(\frac{\theta}{2} - \beta\right).$$

Then, using values of Z_A taken from the system impedance circle for $V_S = V_R$, $I_{IS} =$

$$\frac{V \cos\frac{\theta}{2}}{Z_A \cos(\theta - \alpha)}$$

at the end with outgoing power or

$$\frac{V \cos\frac{\theta}{2}}{Z_B \cos\left(\frac{\theta}{2} - \beta\right)}$$

at the end with incoming power. Referring to Figure 12, with outgoing power

$$I_{12S} = -I_{31S} = \frac{3}{2} I_{IS} = \frac{3}{2} \frac{V \cos\frac{\theta}{2}}{Z \cos\left(\frac{\theta}{2} - \beta\right)}$$

The fault current is $\sqrt{3} V_S / 2 Z_{SB}$ from the sending end to a relay at *B*, and it is $\sqrt{3} V_R / 2 Z_{RA}$ from the receiving end to a relay at *A*. The total relay torque is $V_{pol}(I_S \cos \psi + I_F \cos \lambda)$ where V_{pol} is the polarizing potential of the unit, so that the relay trips when $I_S \cos \psi + I_F \cos \lambda$ is positive. The unit has maximum torque when the current is 30 degrees leading the polarizing potential, so that ψ or λ are 30 degrees less than the angle between I_S or I_F and the polarizing potential.

If one refers to Figure 13, treating counterclockwise angles as positive

$$\psi_{12} = \gamma - 60°$$

$$\lambda_{12} = -90° + \phi + \theta - \alpha - 60° + 180° = \theta + \phi - \alpha + 30°$$

Similarly

$$\psi_{31} = 180° + \gamma - 60° + 90° = \gamma - 150°$$

$$\lambda_{31} = 180° + \phi + \theta - \alpha - 60° + 90° = \theta + \phi - \alpha + 120°$$

The 2–3 relay has no swing component, because $I_{2S} = -{}^1/_2 I_{1S}$; therefore $I_{2S} - I_{3S} = 0$. But

$$\lambda_{23} = 180° - 90° + \phi + \theta - \alpha - 60° - 90° = \theta + \phi - \alpha + 120°$$

Hence, with outgoing power, the relay will trip in any phase where one of the following expressions is positive:

Relay 1–2: $K \cos(\gamma - 60°) + \dfrac{\cos(\phi + \theta - \alpha + 30°)}{Z_{RA}}$

Relay 2–3: $\dfrac{\cos(\phi + \theta - \alpha + 120°)}{Z_{RA}}$

Relay 3–1: $K \cos(\gamma - 150°) + \dfrac{\cos(\phi + \theta - \alpha + 120°)}{Z_{RA}}$

where

$$K = \frac{\sqrt{3} \cos\frac{\theta}{2}}{Z_S \cos\left(\frac{\theta}{2} - \alpha\right)}$$

Referring to Figure 13, in a similar manner, with incoming power, that is, considering the *B* relay

$$\psi_{12} = \gamma - 60°$$

$$\lambda_{12} = 180° - 90° + \phi - \theta + \beta + 30° - 90° = \phi - \theta + \beta + 30°$$

Here again $I_{23S} = 0$ and

$$\lambda_{23} = -90° + \phi - \theta + \beta + 30° + 180°$$

that is

$$\lambda_{23} = \phi - \theta + \beta + 120°$$

Likewise

$$\psi_{31} = -180° - \gamma + 30° = \gamma - 150°$$

and

$$\lambda_{31} = 180° - 90° + \phi - \theta + \beta + 30° = \phi - \theta + \beta + 120°$$

Hence, the relay will trip in any phase where one of the following expressions is positive:

Relay 1–2: $K \cos(\gamma - 60°) + \dfrac{\cos(\phi - \theta + \beta + 30°)}{Z_{SB}}$

Relay 2–3: $\dfrac{\cos(\phi - \theta + \beta + 120°)}{Z_{SB}}$

Relay 3–1: $K \cos(\gamma - 150°) + \dfrac{\cos(\phi - \theta + \beta + 120°)}{Z_{SB}}$

where

$$K = \frac{\sqrt{3} \cos\frac{\theta}{2}}{Z_S \cos\left(\frac{\theta}{2} - \beta\right)}$$

The expression for phase 2–3 has no load term, because the load components in the faulted phases are in phase and hence cancel out. In the case of phase 1–2 with incoming power the swing component becomes positive and tends to trip when θ exceeds 90 degrees, and the fault component tends to trip when θ exceeds 45 degrees. In phase 3–1 the load component always tends to trip and the fault component to open the mho-unit contacts.

With outgoing power both the swing component and the fault component in phase 1–2 tend to prevent tripping. In phase 3–1 the swing component tends to trip and the fault current to prevent tripping up to $\theta = 90$ degrees.

The worst of these cases is the first one, phase 1–2 with incoming power. The Midian relay trips when there are 30 degrees between the station voltages, that is, $\delta = 30$ degrees, and $\theta = 48$ degrees. From a chart similar to Figure 6, $\gamma = 180$ degrees and $Z_S = 620$ ohms. This is checked as follows:

$$K \cos(\gamma - 60°) + \frac{\cos(\phi - \theta + \beta + 30°)}{Z_{SB}}$$

$$= \frac{\sqrt{3} \cos 24° \cos(180° - 60°)}{620 \cos 15°} + \frac{\cos(72° - 48° + 8° + 30°)}{370}$$

$$= \frac{\sqrt{3} \times 0.914 \times (-0.5)}{620 \times 0.966} + \frac{0.47}{370}$$

$$= -0.00132 + 0.00127 = -0.00005$$

This shows that the actual point of failure is slightly more than $\theta = 48$ degrees. It is not worth while checking with a slightly larger value however, because only a slight oscillation would be necessary to reach the larger value.

During the system tests double-ground faults were applied on the 66-kv and 132-kv busses at each end of the line with both incoming and outgoing power up to 35,000 kva, and no wrong operations of the mho units were observed. It was not possible at the time to repeat these tests to check the calculated threshold values.

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Oil-Impregnated-Paper High-Voltage Condenser Bushings for Circuit Breakers and Transformers

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Synopsis: For many years shellac or synthetic-resin-coated paper has been used in winding condenser bushings. Two years ago work on the development of oil-impregnated-paper condenser bushings was undertaken. When a shortage of shellac was imminent because of the war, effort to complete the development was greatly increased, resulting in its being completed well ahead of the date originally set. Manufacturing processes and impregnating treatments were developed and bushings built and tested. The processes and treatments are described, and the data obtained from tests on representative bushings are given.

Complete interchangeability for transformer and circuit-breaker bushings has been obtained for current apparatus designs in the voltage ratings in which the bushings are produced. To a large extent, the new bushings are also interchangeable with bushings of older design in the same voltage rating. The entire insulating portion of the bushing is enclosed in porcelain and metal and this enables the bushing to be held in storage without special provisions for keeping it dry.

The oil-impregnated-paper condenser bushings, built for voltage ratings 92 kv to 230 kv, are susceptible to careful control of quality in the processing operations. Exceptionally consistent performance and high dielectric strength are obtained. Consistently low power-factor readings on both hot and cold bushings were obtained. Radio-influence voltage measurements were below proposed standards.

CONDENSER-TYPE high-voltage bushings 92 kv and above are being built with insulating cores of uncoated high-grade paper wound on the conducting stud and impregnated with low-viscosity insulating oil. Previously, the condenser elements were wound from paper coated with adhesive material such as shellac or one of the synthetic resins.

The condenser construction is old in the art of bushing manufacture. Its ability to withstand both current and voltage surges has been demonstrated amply by its use for more than 30 years in both high- and low-voltage bushings. Therefore, the theory of bushing-condenser design is not repeated in this paper.

The oil-impregnated-paper condenser bushings retain the known advantages of uniform voltage gradient, high-capacitance potential tap, mechanical strength, and small external diameter. From these features accrue other values such as small diameter and high efficiency of current transformers and the possibility of securing the maximum of interchangeability between both new and old circuit-breaker and transformer bushings. Complete interchangeability has been attained between transformer and circuit-breaker bushings of 92-kv and higher-voltage ratings in current production, and although the new oil-impregnated bushings cannot be made interchangeable with all old bushings, they can replace older designs to a greater extent than has been possible heretofore.

The development of oil-impregnated condenser bushings, although accelerated by wartime conditions, has been planned for several years. Plastics and oils were studied. A low-viscosity oil was selected to prevent formation of voids within the condenser at extreme winter temperatures and to facilitate the impregnation process, and also because of a slight superiority in electrical properties of the impregnated paper.[4,7]

The insulation in condenser bushings under normal service voltage to ground, is worked at an average potential stress of less than 50 rms volts per mil. In the 60-cycle withstand test, the gradient is approximately 200 volts per mil. Under surge conditions, with a 1.5 by 40-microsecond full wave, at the basic impulse level, the stress may reach an average value of 450 crest volts per mil of insulation thickness for a few microseconds. Steep-front surge flashovers have been made in the high-voltage laboratory,[1-3] in which the average stress at the time of crest voltage was just under 1,000 volts per mil.

The voltage gradients in these bushings are relatively low compared with those in other applications of oil-impregnated paper insulation. J. B. Whitehead[4] has published accelerated-life-test results for voltage stresses starting at 400 rms volts per mil and increasing 3.12 per cent every four hours until failure. The average failures occurred at about 715 volts per mil, after about 75 hours, in samples comparable to a single layer or step of a condenser bushing wound with 0.005-inch paper. The bushing condensers have an added advantage of being wound from a wide sheet of paper instead of from narrow strips of tape (except that, in the larger sizes, the inner layers more than 100 inches long are helically wound from paper tape).

In paper-insulated oil-impregnated cables,[6] in the same standard voltage ratings as these bushings, the normal operating stress is approximately 140 rms volts per mil. Foust and Scott[7] show that such insulation breaks down at 1,300 to 2,000 crest volts per mil on impulse voltage tests using a 1.5-by-40-microsecond wave.

In oil-impregnated paper capacitors, using capacitor paper and thinner insulating walls, continuous 60-cycle rms gradients of 250 to 300 volts per mil have been used and intermittent d-c gradients up to 1,000 volts per mil.

This experience and test data, substantiated by other laboratory tests omitted for brevity, indicate a large margin of insulation strength in the condenser, even when stressed with the

Table I

Gap	No. of Shots	Corrected Kilovolts	Time to Flashover (Microseconds)	Average Rate of Rise (Kilovolts Per Microsecond)	Effective Rate of Rise (Kilovolts Per Microsecond)
33-inch rod	20	982	0.94	1,045	1,760
		980	0.91	1,070	1,630
42-inch rod	20	1,080	1.6	675	1,080
46-inch experimental	100	1,142	1.84	620	1,220
		1,135	1.75	650	1,250
42-inch rod	20	1,567	0.70	2,240	2,340
42-inch rod	20	1,745	0.61	2,860	3,160

Flashovers occurred sometimes on the gap or bushing alone, but generally the flashover occurred on both bushing and gap.

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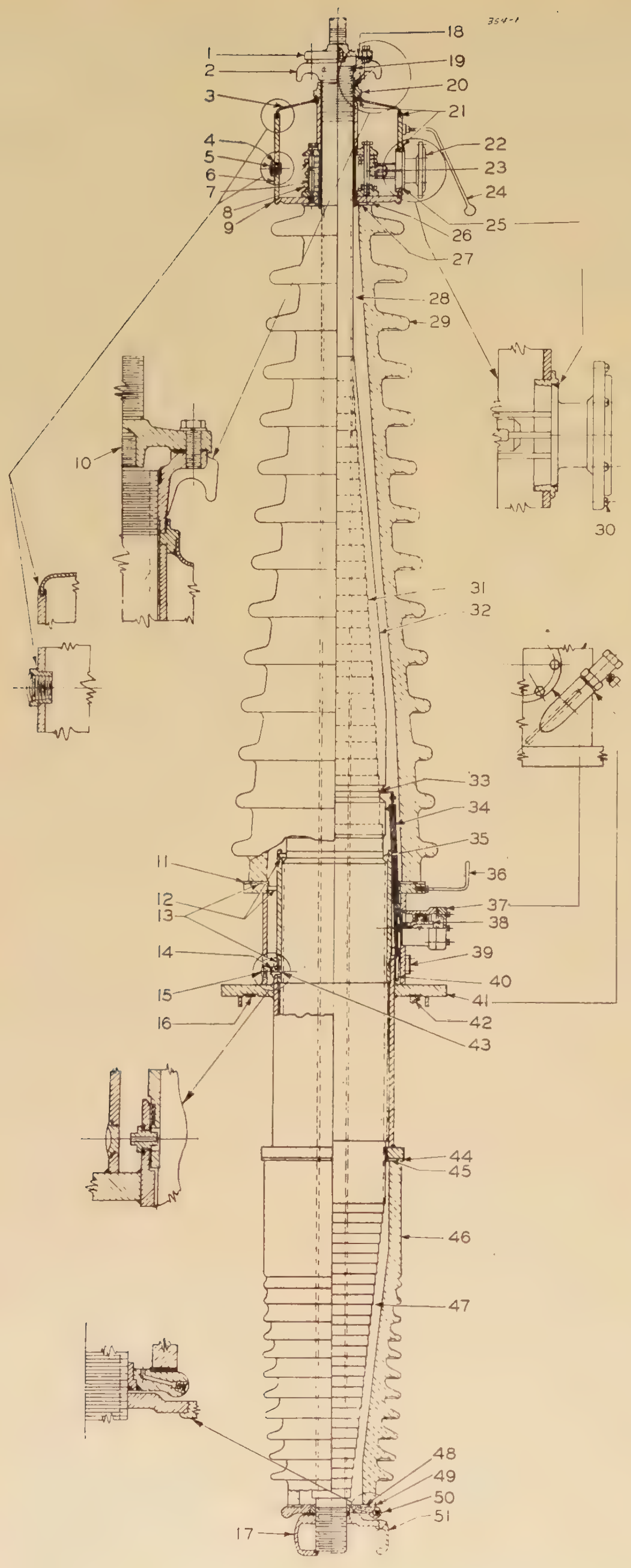

Figure 1. Typical outline of oil-impregnated condenser bushing

1—Adapter cap
2—Nut with lifting lugs
3—Spun copper. Differential expansion taken up by flexing
4—Brazed
5—Pipe plug soldered over
6—Steel shell
7—Gauge for determining spring setting
8—Coil-spring assembly places pressure on all gaskets for upper and lower porcelains. Six springs
9—Welded
10—Connection for fished-through lead for transformer
11—Asbestos Neoprene gasket
12—Grounding straps
13—Cork Neoprene gaskets
14—Stop to limit vertical movement and prevent rotational movement of condenser
15—Low-temperature solder
16—Cork Neoprene gasket for transformer
17—Ball static shield for transformers only
18—Cork Neoprene gasket
19—Low-temperature solder
20—Thimble
21—Brazed
22—Removable magnetic oil gauge—four-inch dial
23—Oil level at 75 degrees Fahrenheit
24—Gap electrode
25—Low-temperature solder—can be unsoldered to remove gauge
26—Asbestos Neoprene gasket
27—Cork Neoprene gasket
28—Upper oil chamber
29—Upper porcelain
30—Gauge dial and pointer assembly can be easily replaced
31—Copper foils
32—Oil-impregnated condenser (oil-filled kraft paper)
33—Voltage tap band
34—Voltage tap connector
35—Ground foil band
36—Gap electrode
37—Voltage tap socket
38—Porcelain
39—Name plate
40—Sampling valve and tubing
41—Steel flange
42—Bevel ring and asbestos Neoprene gaskets for circuit breaker
43—Small hole—connecting passage between upper and lower oil chambers
44—Asbestos Neoprene gasket
45—Cork Neoprene gasket
46—Lower porcelain
47—Lower oil chamber
48—Cork Neoprene gasket
49—Asbestos Neoprene gasket
50—Drain plug
51—If this bushing is used as replacement of different style bushing, use the static shield or ball from the replaced bushing

most severe surges where the average gradient was 1,000 volts per mil.

Successful production of oil-impregnated-paper bushing insulators requires carefully developed and controlled manufacturing processes and treatments. At the same time, this construction is responsive to careful control and treatment during production, resulting in a more uniform product than has been obtained heretofore. A large part of the equipment previously used for winding condensers from coated paper has been adapted, with minor changes, to production of the oil-impregnated condenser. Equipment for vacuum drying and degassing the bushings has been specially developed for this purpose. Oil conditioning and handling equipment likewise is new (Figure 2).

Bushing Construction

Figure 1 shows typical construction of the oil-impregnated condenser bushings, which are built in ratings of 92 to 230 kv inclusive. The conductor is a copper tube upon which the paper condenser is wound and serves as a stay bolt holding the entire assembly together under pressure of compression springs in the cap.

The stud or lead (the copper tube) is prepared by straightening, shot blasting, and coating with an adhesive material. Bare Kraft paper 0.005-inch thick, having a density of 0.82 maximum–0.67 minimum is wound on the stud to the specified diameter. During winding, at precalculated diameters, coated copper-foil sheets are inserted to provide a series of concentric condensers. Each foil is coated on both sides with thermoplastic adhesive which binds the foil to the adjacent layers of paper, and the condenser at intervals of approximately every quarter inch increase in diameter. When the specified number of condenser layers has been wound, a layer of paper treated with adhesive is wound on the outside to bind the condenser structure securely.

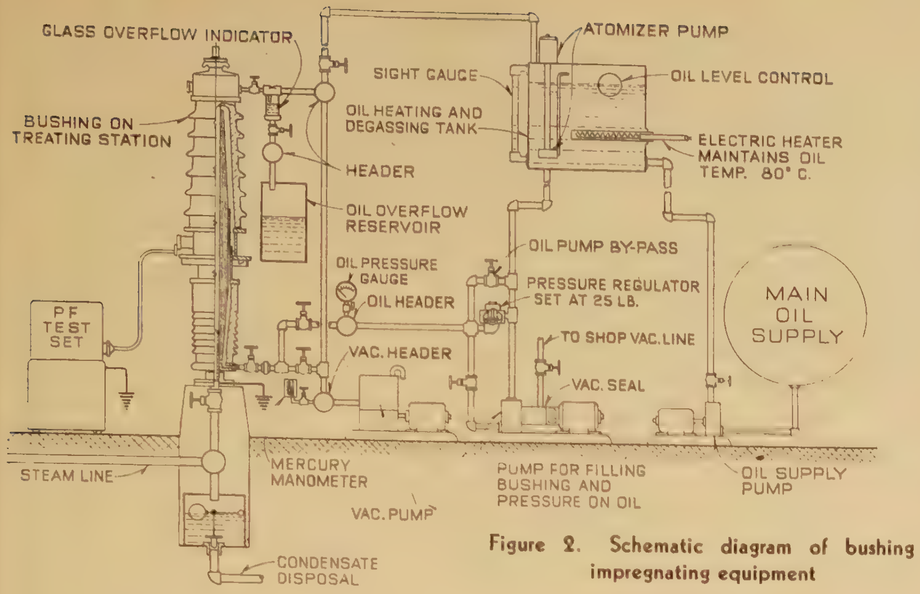

Figure 2. Schematic diagram of bushing impregnating equipment

During the winding operation, the paper is heated to 165 degrees centigrade ±5 degrees centigrade and rolled under tension and pressure. It is then machined in the same manner as the shellac Micarta condenser which it supersedes.

A steel sleeve is pressed on the machined condenser and connected electrically to the outer foil. This sleeve provides a means for attaching and mounting the condenser to the bushing flange, allowing limited movement longitudinally and rotationally between the condenser and the flange, as required for building and for differential expansion and contraction. A fabricated steel flange with provision for a potential tap insulator and oil-sampling valve supports the porcelains which encase both the lower and upper ends of the condenser. Gasketed joints with mechanical stops are provided between the porcelain and metal members. The pressure on all gaskets is equal and is obtained from springs within the cap. The condenser, being permitted to move longitudinally through the flange, within controlled limits, permits use of the same springs to provide pressure on all of the gaskets. All joints in the enclosing parts of the bushing, except the gasketed joints adjacent to the porcelains, are made by silver-alloy brazing or by soldering.

It was found during the impregnating period, when bushings are subjected to hot oil under pressure for several days, that many castings would show leaks, even several days after the treating operation began. Therefore, all metal parts of the bushing which form oil seals are now made from sheet or fabricated material. The insulating condensers are enclosed completely in metal and porcelain so as to remove all possibility of moisture absorption at any time. Oil-level indication is provided by a magnetic-type gauge soldered into the side of the cap. A valve is provided in the flange for obtaining oil samples, if required, and removable plugs in the bottom and top of the bushing permit draining or refilling.

Processing of Bushings

The assembled bushing is sealed and tested for leaks with an internal air pressure of 20 pounds per square inch and the complete bushing immersed in water for 30 minutes. During the test there must be no evidence or appearance of bubbles caused by escaping air. To expedite drying during the vacuum processing, the bushing is baked for approximately 12 hours at 95 degrees centigrade ± 5 degrees centigrade immediately before being connected to the treating system. During processing, the bushing is mounted on a support as shown in Figure 2, and steam at 120 degrees centigrade (approximately 14 pounds per square inch gauge pressure) is admitted to the inside of the copper tube on which the bushing is wound. The top end of the tube is closed and sealed. The steam gives off heat to the bushing, and condensation occurs within the copper tube and is removed from the system through a suitable trap. Connection is made to the vacuum system at both the top and bottom ends of the bushing. An absolute pressure of less than 5 millimeters of mercury is obtained within the bushing. The drying treatment is continued under high temperature and low pressure until degassing is complete and the power factor does not exceed 0.004. When this condition is obtained, a low-viscosity insulating oil, previously conditioned by heating and degassing, is admitted to the bushing at the lower end and permitted to rise until the bushing enclosure is filled completely. Pressure is then applied to accelerate complete saturation of the condenser with oil. Saturation is tested by reading the capacitance of the bushing with a Schering bridge and noting when no further increase occurs. It has been found that the fully treated bushing capacitance increases approximately 44 per cent over the dry bushing capacitance.

Mechanical Tests

Mechanical tests were made on a sample bushing to demonstrate ability to withstand handling, transportation, and service abuses. They are reported in detail in the appendix.

Electrical Tests

Electrical tests made on bushings of two voltage classes are reported in this paper. The first was a 196-kv 1,200-ampere experimental bushing. This bushing differed from standard in two respects:

1. There was no potential tap.
2. The lower porcelain was nine inches shorter than standard.

The short porcelain was used to avoid delaying tests while waiting for the standard porcelain of the new design to be manufactured.

The second bushing tested and described in this paper was rated 138 kv, 1,200 amperes. It was of standard design and materials in all respects.

Test results are given on these two ratings of bushings, because 138 kv is the most widely used transmission voltage, and 196-kv bushings are used on 230, 196-kv apparatus where they are subjected to the 230-kv system voltage.

Electrical tests were made to determine if the bushings would meet all AIEE, National Electrical Manufacturers Association, or other electrical requirements. After these requirements were satisfied successfully, tests were continued for the purpose of obtaining information on the reserve insulation strength. An unusually large reserve was shown in both bushings, as indicated in the complete test results given in the appendix, but summarized in the following paragraphs.

196-Kv Bushing Tests

Before making high-potential tests, power-factor and capacitance measurements were made at room temperature

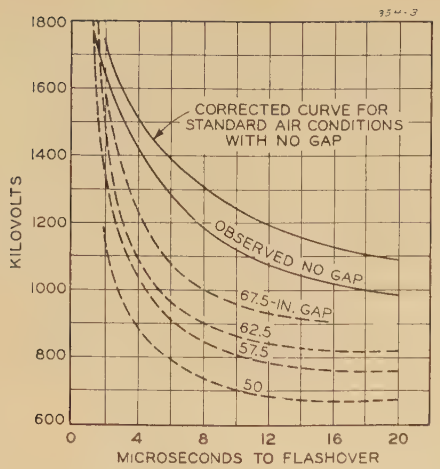

Figure 3. 196-kv experimental bushing flashover curves

Standard rod gap in parallel, 1.5x40-microsecond positive wave.
Approximately 40 flashovers for each curve observed

Weather	Barometer (Inches)	Temperature (Degrees Fahrenheit)		Relative Atmospheric Density	Humidity	Humidity Correction Factor	Total Correction Factor
		Dry	Wet				
50-inch gap	29.26	66	52	1.008	0.238	1.093	1.084
57.5-inch gap	29.16	76	56	0.980	0.235	1.093	1.115
62.5-inch gap	29.10	82.5	59	0.965	0.248	1.088	1.127
67.5-inch gap	29.10	70	53.5	0.985	0.233	1.093	1.109
No gap	29.24	86	62.5	0.965	0.313	1.071	1.109

(25 degrees centigrade) and at elevated temperature (the entire bushing heated to 80 degrees centigrade). The readings were:

25 degrees centigrade—power factor = 0.0049, capacity = 366 micromicrofarads

80 degrees centigrade—power factor = 0.0076, capacity = 371 micromicrofarads

Comparative power-factor and capacitance measurements were made up to 200 kv, using a high-voltage Schering bridge (see appendix). The readings taken before and at intervals during high-voltage testing showed that the curve of power factor and capacitance versus voltage was practically flat, and also that the bushing condenser was not being damaged appreciably by the tests.

Radio-Influence Voltage measurements were taken at 10-kv steps from 160 kv to 200 kv. No measurements were above 25 microvolts. The same values were read on the test circuit alone and with the bushing connected to the circuit. It therefore seems obvious that the value of radio-influence voltage of the bushing was below that of the circuit alone. At the conclusion of all high-voltage tests, after both porcelains had been destroyed and the condenser badly damaged, readings were taken up to 150 kv. No reading exceeded 150 microvolts.

Values of radio-influence voltage that are permissible in high-voltage apparatus have been the subject of study for some years; test methods have been agreed to; and considerable data have been obtained on important apparatus. There is still a question as to how far the industry can go in requiring low radio-influence voltage levels without imposing economic hardship. The form of bushing here discussed is favorable from the point of view of radio interference. This is the result of a bushing construction in which the insulating material, being completely impregnated with oil, is free from voids, and the discharges which produce radio-influence voltage are minimized.

One-Minute Dry Withstand Tests, 60 cycles, 465 kv (AIEE Standard) were made at the beginning of, during, and at the completion of the high-potential test program. The bushing did not give any indication of distress, although both porcelains were broken and considerable damage done to the condenser before the last withstand test was made.

The Standard Impulse Test for bushings of this voltage rating is three shots with a 1.5 by 40-microsecond full wave having a crest value of 900 kv. After the bushing was subjected to the standard test, additional impulse tests were made with positive and negative 1.5x40-waves having crest values ranging from 680 kv to 1,800 kv. A total of 203 impulses was imposed on the bushing. The values provided volt-time impulse-flashover curves shown in Figure 3. Each curve represents approximately 40 shots. In this set of tests, gaps were used in parallel with the bushing for the first 160 tests, as indicated in Figure 3. Forty tests were then run without a gap. In Figure 3 these tests are marked "no gap." When corrected for standard air conditions, the actual impulse-flashover values are indicated by the curve in Figure 3 marked "corrected curve for standard air conditions with no gap." Three 1.5 by 40-microsecond negative full-wave impulse tests were made. The third test broke a skirt of the lower porcelain. To conserve the porcelains for additional testing, gaps were again installed, and 77 steep-front impulse tests were made having crest values from 1,178 kv to 1,850 kv. During this series of tests, both porcelains were broken.

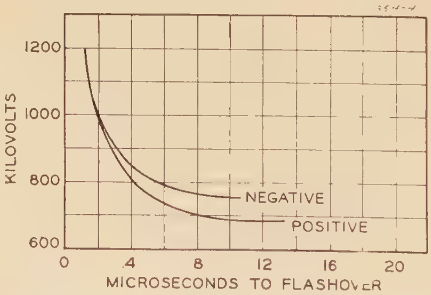

Figure 4. 138-kv bushing flashover curves with 43.2-inch standard rod gap in parallel, 1.5x40-microsecond positive and negative waves

Type-O condenser bushing

Weather	Barometer (Inches)	Temperature (Degrees Fahrenheit)		Relative Atmospheric Density	Humidity	Humidity Correction Factor	Total Correction Factor
		Dry	Wet				
Positive	29.34	85	74	0.965	0.732	0.975	1.012
Negative	29.34	85	74	0.965	0.732	0.975	1.012

Table II

End Dropped	Drop Distance (Inches)	Position of Potential Tap	Number of Drops	Remarks
Top	$1^1/_8$	Up	105	No damage*
Top	$^5/_8$	Up	1,100	No damage†
Top	$^5/_8$	60 degrees clockwise	1,000	No damage†
Top	$^5/_8$	60 degrees counter-clockwise	1,000	No damage†
Bottom	$^5/_8$	60 degrees clockwise	1,000	No damage‡
Bottom	$^5/_8$	Up	1,000	No damage‡
Bottom	$^5/_8$	60 degrees counter-clockwise	3,000	No damage‡
Top	$^5/_8$	60 degrees counter-clockwise	2,000	No damage†

* Drop of $1^1/_8$ inch unjustifiably severe; $^5/_8$-inch drop comparable with similar tests previously made on samples of earlier types of bushings.

† Bottom end rebounded about $^1/_8$-inch high after each drop of top end.

‡ Top end rebounded twice (first bounce $^3/_{16}$ inch) at each drop of bottom end. Bottom end also rebounded slightly.

The condenser was apparently unharmed at the end of the tests just described. It was then removed from the porcelain enclosure and left standing with the upper end exposed to air from Friday

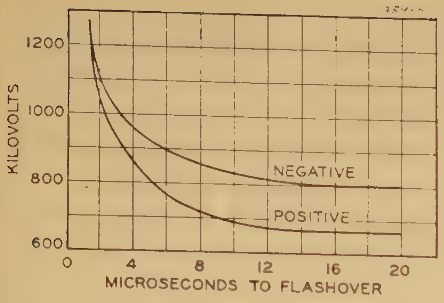

Figure 5. 138-kv bushing flashover curves with 46-inch special gap in parallel, 1.5x40-microsecond positive and negative waves

Type-O condenser bushing, 4$\frac{1}{4}$-inch diameter toroid gap

Weather	Barometer (Inches)	Temperature (Degrees Fahrenheit) Dry	Wet	Relative Atmospheric Density	Humidity	Humidity Correction Factor	Total Correction Factor
Positive	29.36	83	71½	0.970	0.656	0.990	1.031
Negative	29.32	81	74	0.972	0.770	0.969	0.996

night until Monday morning. The entire condenser was then immersed in a large tank of oil and given 40 impulse shots with a 67.5-inch gap in parallel. Voltages having a crest value of 1,880 kv were attained. This set of tests was followed by 25 steep-front impulses having a crest value of 1,900 kv. At the end of this series of tests, damage was indicated in 18 of the 30 steps in the condenser which, however, successfully withstood a 465-kv 60-cycle test for one minute. More detailed information is given in the appendix.

138-Kv Bushing Tests

Power-Factor and Capacitance measurements were made at 13.8 kv with the inverted Schering bridge. The power factor was 0.0037, and the capacitance was 399 micromicrofarads. High-voltage power-factor tests and capacitance measurements were made with a laboratory-type high-voltage Schering bridge. These measurements began at 47 kv and ended at 150 kv. The power factor at 47 kv was 0.0035, and the capacitance was 362 micromicrofarads. At 150 kv, the power factor was 0.0039, and the capacitance was 362 micromicrofarads. The high-voltage Schering bridge tests were repeated several times during the series of 60-cycle and impulse tests. No variation in these values occurred throughout the series of tests, indicating that the condition of the bushing at the end of the tests was substantially the same as at the beginning.

Radio-Influence Voltage measurements were made at several voltages up to 150 kv. No reading exceeded 150 microvolts, indicating that the radio-influence voltage was well within any limits proposed for bushings of this voltage rating.

Dry Flashover Tests. After the standard AIEE one-minute dry test (335 kv), had been made, it was decided to develop the dry flashover strength of the bushing. Ten flashovers were made from 403 kv to 438 kv.

Wet 60-Cycle Flashovers were made following the ten-second wet withstand test of 275 kv. Ten flashovers were made with voltages ranging from 340 kv to 369 kv.

Volt-Time Impulse Tests were made at the standard value of 650 kv with a 1.5x40-microsecond full wave. To obtain further data on the actual insulation strength of the bushing, additional impulse tests were made with positive and negative waves. A total of 160 positive and negative wave impulses were made with a 1.5 by 40-microsecond wave, both full and chopped, from the results of which Figures 4 and 5 were drawn. These tests were followed by 180 steep-wave-front flashover tests. Crest voltages ranged from 982 kv to 1,745 kv. Additional data on this series of tests are given in the appendix.

The insulation of the bushing was indicated to be unharmed at the end of this series of tests, and the bushing was taken to another laboratory for mechanical tests which are described in the appendix.

Conclusions

High-voltage outdoor-type condenser bushings have been built in which the insulation is oil-impregnated paper. The bushings are usable and interchangeable on either circuit breakers or transformers of current design and in nearly all cases can be used to replace bushings of earlier design. The bushings are assembled completely and treated in the factory, then permanently sealed before shipment. They are designed to pass through bushing-mounting flange openings and current transformers without the dismantling of any parts, since the entire condenser structure is hermetically sealed in metal and porcelain. No special storage requirements for protection against weather or atmospheric conditions are required. The design provides for emergency operation of a bushing over a considerable period of time, even though one of the porcelain casings might be broken or destroyed. Electrical tests required by the industry have been met with a wide margin of safety. Very high puncture strength has been demonstrated by the fact that no damage was done to the insulating members over a long series of electrical tests until after the enclosing porcelains had been completely destroyed. Power-factor measurements at both room temperature and elevated temperature are very low. Radio-influence voltage measurements were well within limits proposed.

The tests reported in this paper were made on new bushings under good conditions. Bushings in service cannot be kept clean at all times and should not be expected to maintain continuously the high values obtained in these tests.

Since 1936 the Detroit Edison Company has rebuilt a large number of 132-kv condenser bushings using the old condensers. Before the rebuilding, the varnish was removed, and the condensers were impregnated with insulating oil. The satisfactory service record of these rebuilt bushings, with the limitations of old materials, is convincing evidence that the use of oil-impregnated paper condensers is a valuable improvement in bushing construction.

While the construction and processing of the rebuilt bushings and those described in this paper are not identical, the authors received valuable encouragement and stimulation from the early work done by the Detroit Edison Company engineers in rebuilding bushings.

Appendix

Detailed Report of Special Tests on 196-Kv Experimental Bushing

Test 1—High-Voltage Schering-Bridge Tests at 200 Kv. Comparison standard capacitor comprising a stack of coupling capacitors, as read by 13.8-kv inverted Schering bridge; 0.0024 power factor, 642 micromicrofarads.

Apparent power factor and capacitance of bushing, mounted in large closed-top testing tank of oil, at 200 kv, assuming comparison standard power factor and capacitance to remain constant: 0.00305 power factor, 297.5 micromicrofarads.

This reading was rechecked, with insignificant small variations, at frequent intervals during the surge-testing program, and this served to prove that no appreciable damage was being inflicted on the bushing condenser by the tests.

This reading neglects the unknown difference between the capacitance of the comparison standard as read by the inverted Schering bridge and as used in the conventional high-voltage bridge circuit, because of the external capacitance to ground being included in the former and excluded in the latter.

Test 2—Radio-Influence Voltage Measurements to 200 Kv. Test made with standard radio-influence measurement circuit,[8,9] using 220-kv coupling capacitor.

Applied 60-Cycle Kilovolts	160	170	180	190	200
Microvolts, with bushing	10	25	15	20	15
System, without bushing	10	25	15	20	15

Test 3—Dry Withstand Test, 465 kv, 60 cycles, one minute—successful. (Schering-bridge check test at 200 kv.)

Test 4—Impulse-Flashover Curve, kilovolts versus microseconds 1.5 by 40 microseconds; positive wave, 50-inch standard rod gap in parallel. 680 kv to 1,150 kv at 2 microseconds. See Figure 3 (40 shots)—successful. (Schering-bridge check test at 200 kv.)

Test 5—Impulse-Flashover Curve, kilovolts versus microseconds 1.5 by 40 microseconds; positive wave, $57\frac{1}{2}$-inch standard rod gap in parallel. 770 kv to 1,800 kv at 1.2 microseconds. See Figure 3. (40 shots)—successful.

Test 6—Impulse-Flashover Curve, kilovolts versus microseconds 1.5 by 40 microseconds; positive wave, $62\frac{1}{2}$-inch standard rod gap in parallel. 820 kv to 1,650 kv at 2 microseconds. See Figure 3. (40 shots)—successful.

Test 7—Impulse-Flashover Curve, kilovolts versus microseconds 1.5 by 40 microseconds; positive wave, $67\frac{1}{2}$-inch standard rod gap in parallel. 900 kv to 1,650 kv at 2 microseconds. See Figure 3 (40 shots)—successful. (Schering-bridge check test at 200 kv.)

No damage of any kind to the bushing resulted from any of the foregoing tests.

Test 8—Impulse-Flashover Curve, kilovolts versus microseconds 1.5 by 40 microseconds; positive wave without gap in parallel. Approximately 40 shots. 940 kv to 1,770 kv at 0.75 microsecond. See Figure 3—successful. (Schering-bridge check test at 200 kv.)

In test 8, a small piece was broken off the edge of one skirt near the middle of the weather casing.

Test 9—Impulse-Flashover Curve, kilovolts versus microseconds 1.5 by 40 microseconds; negative wave without a gap in parallel—test not completed. (Schering-bridge check test at 200 kv.)

After three shots, a skirt of the lower porcelain was broken by flashover in the tank oil, and negative-wave flashover tests were discontinued. The standard porcelain for this 196-kv bushing is $36\frac{1}{4}$ inches long, but to avoid waiting for manufacture of the correct porcelain, this experimental bushing had been built with a porcelain $27\frac{1}{8}$ inches long. Therefore, performance of this lower porcelain was not pertinent. With the broken lower porcelain, the testing was resumed.

Test 10—Steep-Front Impulse Test, positive wave, 63-inch standard rod gap in parallel. Ten shots 1,178 kv (uncorrected). Effective rate of rise kv per microsecond = 870. Flashed on gap.

Test 11—Same as Test 10 except 1,709 kv (uncorrected), and effective rate of rise = 3,200 kv per microsecond. Ten shots. Eight shots flashed gap and two shots flashed both gap and bushing.

Test 12—Same as Test 11 except 1,850 kv (uncorrected) and 3,360 kv. Per microsecond effective rate of rise. 57 shots. All shots flashed both gap and bushing.

Noises were heard in the test tank on 36th and 37th shots, indicating further shattering of the previously broken lower porcelain. Bridge readings were taken and found to be normal. Testing was resumed on the following day. On the 56th shot, a small piece was broken off the top end of the top porcelain. The 57th shot was taken, and a large piece was knocked out of the upper porcelain extending down to eighth skirt from the top. Apparently the bushing had lost oil because of breakage of lower porcelain, since none was found in weather casing when it broke.

The bushing was temporarily lifted above the test tank for inspection, and the lower porcelain was found badly shattered.

Bridge readings were taken at 200 kv and indicated the condenser to be unharmed. (0.0034 power factor; 297.5 micromicrofarads.)

The bushing was removed from the test tank.

The porcelains and flange were completely removed and the condenser examined. Slight markings were observed on the surface of the lower end, indicating that after the lower porcelain was broken, at least one flashover had occurred over the surface of the lower end of the condenser.

The unprotected condenser was left standing from Friday night until Monday morning with the upper end exposed to air, the lower end, from the normal flange location down, was immersed in oil. Without further treatment, test 13 was made.

Test 13—The condenser was suspended on a string of strain insulators in a 20-foot-diameter oil tank with the outer foil grounded. To reduce voltage disturbances over the surface of the oil, the cap was used as a shield on the upper end of the stud, about half of the cap being under oil. The condenser was given the one-minute dry 60-cycle withstand test of 465 kv. Results—successful.

Test 14—Still in oil tank, as in test 13, an impulse curve, kilovolts versus microseconds, to flashover was run with a 1.5 by 40-microsecond positive wave and a $67\frac{1}{2}$-inch rod gap in parallel to 1,880-kv crest. About 40 shots were taken. The curve corresponded closely to the curve for the $67\frac{1}{2}$-inch gap in Figure 3. Results—successful.

Test 15—With the same setup as for test 14, 25 shots were taken with a positive wave rising to 1,900-kv crest. Effective rate of rise 3,360 kilovolts per microsecond. During the series, some bubbles rose, indicating breakdowns were occurring either over or through the condenser insulation. On the 21st to 25th shots, the gap did not flash. Inspection showed 18 of the 30 steps in the condenser damaged.

Test 16—The condenser was again suspended in the tank as for tests 14 and 15. 465 kv, 60 cycle was applied for one minute. The badly damaged condenser passed the regular 60-cycle withstand test.

Test 17—The condenser was unwound, and all the breakdowns were observed to have occurred at the ends of the copper foil cylinders, some at the top and some at the bottom end. Each breakdown burned through or flashed over one to three steps. The ends of the bushing had been exposed to the air after the removal of the porcelain casings. The paper was found thoroughly saturated with oil throughout the condenser.

Detailed Report of Special Tests on 138-Kv 1,200-Ampere Bushing

The bushing tested was of standard design and materials in all respects. The processing equipment for production was not completed, and processing was done on experimental equipment. Tests as described in the following were made successfully:

Test 1—Bushing Mounted in Test Tank, room temperature:

(*a*). 13.2-kv inverted Schering bridge = 0.00375 power factor
Capacitance = 399 micromicrofarads
(*b*). High-voltage Schering bridge 47 kv = 0.0035 power factor
Capacitance = 362 micromicrofarads
(*c*). 150 kv = 0.00395 power factor
Capacitance = 362 micromicrofarads

Test 2—Dry 60-Cycle Flashovers (corrected to standard atmospheric conditions):

438 kv—411 kv—438 kv—432 kv—418 kv
426 kv—430 kv—416 kv—403 kv—410 kv

Test 3—Wet 60-Cycle Flashover (corrected kilovolts):

340 kv—353 kv—353 kv—350 kv—357 kv
362 kv—369 kv—357 kv—353 kv—359 kv

Precipitation—0.2 inch per minute
Water pressure—40 pounds per square inch
Resistivity—7,000 ohms per inch cube

Test 4—Schering-Bridge Test to 150 kv.

Test 5—Time to Flashover Impulse Curve with special toroid gap set at 46 inches in parallel, to flash at 670 kv, 1.5 by 40-microsecond positive wave—40 shots up to 1,200 kv. See Figure 5.

Test 6—Time to Flashover Impulse Curve with standard rod gap set at 43.2-inch (700 kv) 1.5 by 40-microsecond positive wave, 40 shots up to 1,200 kv. See Figure 4.

Test 7—Same as Test 5 except with negative wave. See Figure 5.

Test 8—Same as Test 6 except with negative wave. See Figure 4.

Test 9—Schering-Bridge Test at 150 kv indicated no damage.

Test 10—Steep Wave-Front Flashover Tests. For typical oscillograph data see Table I.

Test 11—Schering-Bridge Tests up to 150 kv, together with inspection, showed the condition of the bushing to be the same as at the beginning of the test program.

Drop Tests on 138-Kv 1,200-Ampere Bushing

Bushing horizontal, supported on fulcrum at one end and motor-operated lifting and dropping mechanism at other. Drops 21.5 times per minute (1,290 per hour). See Table II.

Elastic Bending Deflection of Bushing ($\frac{5}{8}$-Inch Drop)	At Top End of Flange	At Bottom End of Flange
When dropping top end	$\frac{1}{8}$ inch	$\frac{1}{8}$ inch
When dropping bottom end	$\frac{3}{16}$ inch	$\frac{1}{8}$ inch

Performance Requirements for Relays on Unusually Long Transmission Lines

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Synopsis: Analysis of fundamental bases of relaying made as part of the engineering for the 270-mile 154-kv interconnection between systems of Nebraska Power Company and Kansas Gas and Electric Company reveals that, of current, phase angle, impedance, and impedance angle, none alone offers satisfactory discrimination among open-circuit, short-circuit, and load conditions. Combination of input-impedance magnitude Z and impedance angle $\psi=\tan^{-1}X/R$ is studied by means of "impedance circle diagrams" of the interconnecting circuit. On these diagrams is charted the dynamic behavior of the interconnected systems during faults and surges of synchronizing power. The desirable areas of trip and no trip are mapped, and general specifications for the performance of relay systems are presented in terms of impedance magnitude and angle, $Z\angle\psi$. Thereby full advantage can be taken of all possible synchronizing power inherently available over the interconnection and at the same time faults and approach of out-of-step condition can be recognized properly.

RE-EXAMINATION of fundamental principles of transmission-line relaying for phase protection and the development of advanced methods for calculating and specifying relay performance for long lines and interconnections have been included in the engineering of a 270-mile 154-kv interconnection between systems of Nebraska Power Company and Kansas Gas and Electric Company.[1] This has led to development of new long-line-type relays described in a companion paper.[2] Installation and test of these relays under conditions of actual operation are described in another companion paper.[3]

The complete relay requirements for this interconnection are quite special, because the transmission line is unusually long; transformers are connected as parts of the line between terminal breakers, and automatic instantaneous reclosure of circuit breakers is employed to maintain synchronism after faults without benefit of other parallel ties.

Bases for Relaying

As introduction to the problems of relaying this transmission circuit, some of the principal bases of modern relaying and their applicability to long-line interconnections are reviewed.

Current Magnitude and Phase-Angle Bases

Figure 2 indicates as a function of length the magnitude of current which may be expected at the sending end of an open-wire transmission line with construction suitable for 154-kv 60-cycle operation for open circuit, for a representative load, and for short circuit. Study of these curves reveals that there is little or no hope of attaining discrimination between load and short circuit by measurment of current magnitude on lines much in excess of 100 miles in length. This virtually rules out consideration of overcurrent relays for long lines. On the 270-mile line under study, there would be absolutely no margin since the current on load swings exceeds the short-circuit current for faults at the far end of the line. It should be noted also that the line-charging current is nearly equal to the minimum short-circuit current.

In this discussion, a representative load is assumed to be an impedance equal to the characteristic impedance or surge impedance of the line,[4] $\boldsymbol{Z_0}=\sqrt{z/y}$. However, as explained in the paragraphs which follow, momentary requirements for transfer of synchronizing power between large systems easily may result in tie-line current considerably greater than surge-impedance load and even in excess of far-end short-ciacuit current.

The phase-angle function in transmission-circuit performance and its relation to relaying can be studied conveniently by beginning with the power circle diagram representing power input to the circuit terminal at the relay location. This diagram is plotted in Figure 3 from the sending-end power equation 1

$$P_s=\frac{E_s^2\cdot D}{B}\underline{/(\Delta-\beta)}-\frac{E_sE_r}{B}\underline{/(\delta_{rs}+\beta)}$$

The form of this equation, and the notation used, are explained fully in Appendix I.

The first term of this equation depends only upon the circuit constants and the voltage E_s at the near end or sending end

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Deflection Tests of 138-Kv 1,200-Ampere Bushing

Bushing mounted vertically by its flange. Sidewise stress applied, and deflection measured to left and to right, seven times each, first at top end and then at bottom end of bushing. Deflections apparently were entirely elastic up to 220 pounds maximum applied.

Deflection per 100 pounds side stress:
Top end—0.00625 inch. Bottom end—0.0052 inch.

Vibration Test

Motor with flywheel unbalanced 0.474 pound-foot, attached to top of bushing, was run 84 hours at 1,300 rpm, exerting a centrifugal force of 273 pounds, vibrating the top of the bushing to a deflection of approximately 0.014 inch from the center line. No oil leak or other damage resulted.

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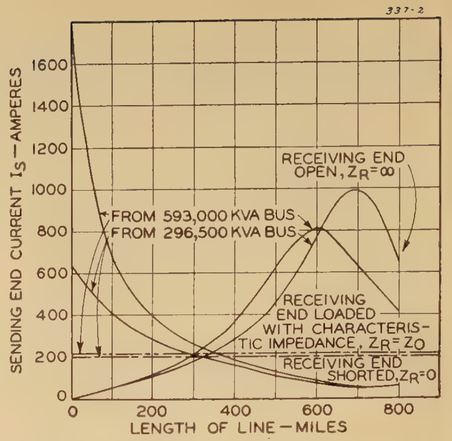


Figure 2. Current input to line from 154-kv bus as a function of length and far-end terminal load

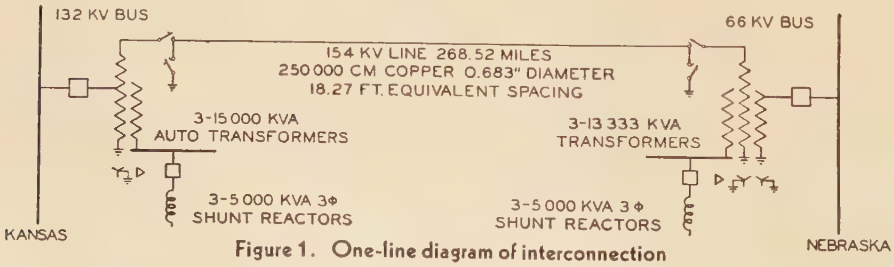


Figure 1. One-line diagram of interconnection

of the circuit and defines the location of the center 0′ of the power circle. The second term defines the magnitude and direction of the radius of the power circle.

When $E_r < DE_s$, the power circle radius E_sE_r/B becomes less than E_s^2D/B, which is the distance 0-0′ of the center to the origin, then the steady-state side of the locus P_s lies between the origin 0 and the center of the power circle 0′. With these conditions, the power-factor angle θ lies in the same general direction at normal loads as it does for short circuits on the line.

A numerical example will make this effect clear. For a line about 150 miles long, of uniform symmetrical construction and without terminal transformers, $D=A=0.95$ (see Figure 8). Assume first that $E_r=DE_s$, which means that far-end or receiving voltage is 95 per cent of the near-end or sending voltage. This is a normal condition. The power circle for this condition passes through the origin of the diagram ($P=0$, $Q=0$). For zero power flow ($P=0$), the power-factor angle θ is indeterminate. For small flow of power from the bus to the line ($P>0$), θ is nearly equal to $90-\beta+\Delta$ degrees. For a small reverse flow of power ($P<0$), θ is slightly greater than $270-\beta+\Delta$ degrees. This is a change of nearly 180 degrees in the value of θ for a very small change in load over the line which may be a tie between two systems operating in synchronism.

Next consider what happens upon a change in the relative voltage levels at the two ends of this same line. Assume now that $E_r=0.90E_s$. Since $E_r<DE_s$, the power circle for this condition crosses the vertical axis Q $-Q$ *below* the origin. For condition of no power flow ($P=0$), the power-factor angle θ is 270 degrees, meaning that the near-end or sending system is supplying reactive volt-amperes ($-Q$) to the line and to the far-end or receiving system.

Assume last that $E_r=E_s$. Since $E_r>DE_s$, the power circle for this condition crosses the vertical axis Q $-Q$ *above* the origin. For the condition of no power flow ($P=0$), the power-factor angle is 90 degrees, meaning that the near-end or sending system is obtaining reactive volt-amperes (Q) from the line equal to the charging kilovolt-amperes of half the line.

From this example, it may be seen that the power-factor angle θ may swing to any position within a complete circle of 360 degrees under normal load conditions. It therefore cannot be used alone as basis for discrimination between load and fault conditions.

To complete this analysis of phase angle, it is necessary also to investigate cases of short circuit, open circuit, and loads approaching stability limits.

Points corresponding to open circuit at the far end are not readily plotted on the near-end or sending power circle diagram until the sending-end power is determined from some other equation. The power for this condition is $P_s=E_s^2(C/A)$, which is the charging kilovolt-amperes of the circuit. For uniform open-wire lines, this falls in the first quadrant of the power circle diagram for all lengths up to $^1/_4$ wave length, or nearly 760 miles for 60-cycle power lines having low-resistance conductors. Thus the power-factor angle θ for this open-circuit condition on such lines is always less than 90 degrees and for most lines less than 600 miles long is very near 90 degrees.

For a short circuit at the far end of a circuit ($E_r=0$), the power $\boldsymbol{P}_s$ at the near end is found directly from equation 1,

$$\boldsymbol{P}_s = \frac{E_s^2 \cdot D}{B}\ \underline{/(\Delta-\beta)} - \frac{E_s \cdot E_r}{B}\ \overline{/(\delta_{rs}+\beta)}.$$

Since $E_r=0$, the second term of this equation disappears, and the power is the first term which locates the *center* of the power circle 0′. Since all such centers for all values of E_s and for all lengths of uniform lines up to very nearly $^1/_4$ wave length, say to 700 miles for 60-cycle power lines having low-resistance conductors, fall in the fourth quadrant, the power-factor angle θ for short circuits on such lines is always between 270 and 360 degrees and is $\underline{/(\Delta-\beta)}$, corresponding to the length of line to the point of fault.

For analysis of cases near the stability limits of a circuit, it is necessary to refer again to the power circle diagram and power equation. For any pair of values of E_s and E_r, there is one near-end or sending power circle. The maximum power ($P_s$max) which can be put *into* the circuit from the near-end bus at these voltages is specified by a tangent to the power circle drawn to the right-hand side of, and parallel to, the Q $-Q$ axis. The maximum power ($-P_s$max) which can be taken *from* the circuit into the same bus is specified by a similar parallel tangent drawn to the left-hand side of the Q $-Q$ axis.

These two values of power ($P_s$max) and ($-P_s$max) are not steady-state power limits, as might be thought at first glance. The true steady-state power limits for an interconnection established by such a circuit would ordinarily be less than the values thus indicated, depending upon characteristics of the systems interconnected.[5] However, power loads of these magnitudes may be imposed momentarily on interconnection circuits during power swings following system disturbances. Figure 5 illustrates the moment-to-moment changes in angle between two systems and the two terminal voltages of the interconnecting circuit during a system disturbance as obtained from an a-c network analyzer.[6] The angle δ_{rs} is the link or factor connecting this figure with the power circle diagram, Figure 3. It is the angle between the terminal voltages E_r and E_s and must not be confused with either the phase angle θ between current and voltage or the impedance angle $\psi=\tan^{-1}(X/R)$, used in power and impedance diagrams respectively. This angle δ_{rs} is so important in the subsequent discussions that calibration scales of it are included in many of the figures, and diagrams are included for the purpose of identifying corresponding points as related by this angle throughout these discussions.

Referring again to the power limits on the power diagram, Figure 3, it is seen that on the upper or stable half of those

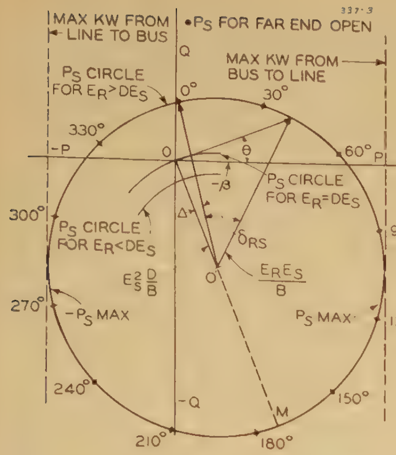


Figure 3. Construction of sending-end power circle diagram from equation 1

power circles which enclose the origin 0, that is, circles corresponding to $E_r > DE_s$, the phase angle θ swings continuously in one direction through more than 180 degrees as the power simultaneously swings through its full range from ($P_s$max) to ($-P_s$max). For fixed near-end voltage E_s and at one or both of the loads, ($P_s$max) and ($-P_s$max), the kilovolt-amperes and the current I_s measured at the near-end terminal of the circuit are greater than the kilovolt-amperes and current at the same terminal in case of short circuit at the far end.

The very wide range of power-factor angles θ and the lack of discrimination between magnitude of load and fault current previously discussed make overcurrent, directional, and volt-ampere relay elements unsuitable for the phase-to-phase protection of long lines when used alone, in combination with each other, or in combination with impedance-magnitude elements. This is likewise true for any tie circuit whether long or short, if the impedance of the tie is large compared with that of the systems. In other words, it is true in any case where the synchronizing power required to hold systems in step causes large angular separation (δ_{rs}) between terminal bus voltages.

INPUT-IMPEDANCE MAGNITUDE AND ANGLE

It is most helpful and enlightening also to look at the general transmission circuit from the impedance viewpoint, which is probably better understood by communication engineers than by most power engineers. The 60-cycle input impedances of uniform lines with a conductor size and spacing suitable for 154-kv operation are shown for all lengths from 0 to 800 miles in Figures 6 and 7. The three curves of Figure 6 give the magnitude of sending-end impedance with receiving-end open-circuited, loaded with surge impedance $\boldsymbol{Z}_o = \sqrt{z/y}$, and short-circuited respectively. The three curves of Figure 7 similarly give the angles $\psi = \tan^{-1}(X/R)$ of these same impedances.

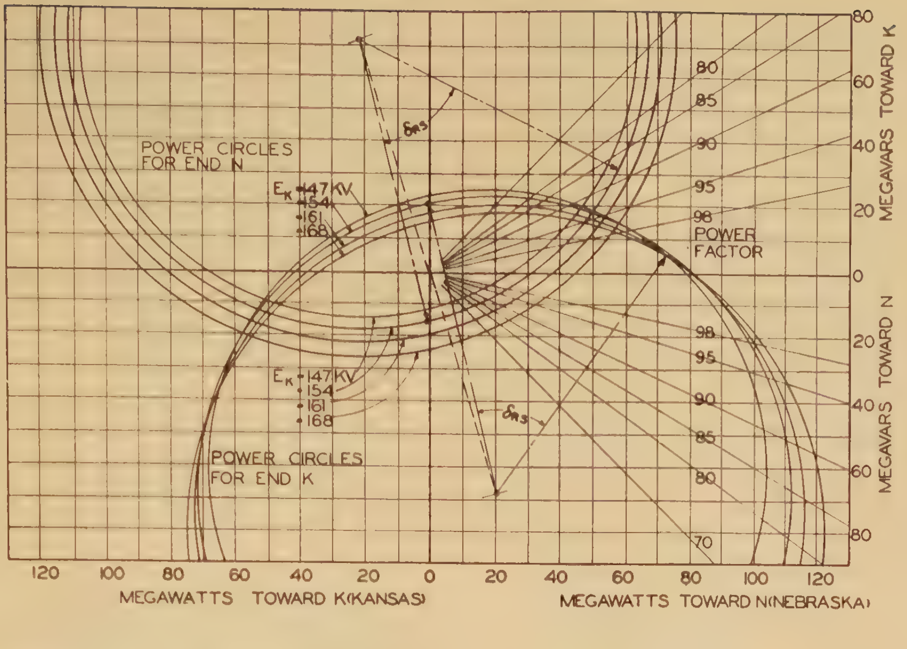


Figure 4. Power circle diagram of the 154-kv interconnection for condition $E_n = 154$ kv

Study of Figure 6 reveals that impedance magnitude alone, or measurement of ohms, does not give a good basis for successful discrimination between open, load, and short-circuit conditions close to the far ends of radial-feeder circuits having electrical length corresponding to more than about 200 miles of open-wire line. In contrast to this conclusion, study of Figure 7 shows that angle of the input impedance is radically different, depending upon far-end conditions. This suggests that measurement of *circuit-impedance angle*, $\psi = \tan^{-1}(X/R)$, should provide a basis upon which successful discrimination can be made between normal and fault conditions and upon which relays can be designed for long lines, possibly up to 600 miles in length. A little further consideration, however, indicates that this basis may also have limitations. The derivations of the impedance formula given in Appendixes II and III indicate that, for each point on the power circle diagram of Figure 3, there is a corresponding impedance magnitude and, further, that the impedance angle $\psi = \tan^{-1}(X/R)$ is the conjugate of the power-factor angle θ. We have already discussed limitations of power-factor angle θ as a relay quantity and, because of the conjugate relation, can infer that some of the same limitations might apply to the impedance angle ψ.

Combinations of impedance magnitude, impedance angle ψ, and impedance components present possibilities as bases for relaying which must be explored. For analysis of these, it is necessary, or at least very desirable, to have circuit characteristics and performance completely expressed in chart form on resistance and reactance co-ordinates.[7]

Construction and Interpretation of Impedance Circle Diagrams

There are certain well-known advantages of impedance as a basis for relaying. It is independent of system voltage, and except in special cases, is independent of the number and location of generators in service, and is primarily dependent only upon circuit constants. Hence it was decided to study further the possible combinations of impedance magnitude and angle to determine the performance which would be required of impedance and reactance relay elements to protect properly the proposed circuit and to find what modifications, if any, would be necessary in conventional relay elements.

In this attack on the problem, it was desirable to obtain general formulas for the sending- and receiving-end impedances in terms of the *ABCD* general circuit constants and the sending and receiving voltages. With this goal in mind, the following equations for near-end or sending-end impedance in terms of the circuit constants and voltages were used:

$$\boldsymbol{P}_s = \frac{E_s{}^2 \cdot D}{B} \underline{/(\Delta - \beta)} - \frac{E_s \cdot E_r}{B} \overline{/(\delta_{rs} + \beta)} \qquad \text{(see Appendix I)} \quad (1)$$

$$\boldsymbol{Z}_s=\frac{(\text{conj }\boldsymbol{E}_s)\cdot\boldsymbol{E}_s}{\boldsymbol{P}_s} \tag{2}$$

$$\boldsymbol{Z}_s=\frac{D\cdot B}{D^2-(E_r/E_s)^2}\overline{/(\Delta-\beta)}-$$

$$\frac{B(E_r/E_s)}{D^2-(E_r/E_s)^2}\underline{/(\phi-\beta)} \tag{3}$$

This was obtained by substituting equation 1 in equation 2 as is developed in Appendixes II and III.

Similarly the following equations in terms of far-end or receiving quantities also are useful.

$$\boldsymbol{P}_r=\frac{E_s\cdot E_r}{B}\underline{/(\delta_{rs}-\beta)}-\frac{E_r{}^2\cdot A}{B}\overline{/(\beta-\alpha)} \tag{4}$$

$$\boldsymbol{Z}_r=\frac{(\text{conj }\boldsymbol{E}_r)\cdot\boldsymbol{E}_r}{\boldsymbol{P}_r} \tag{5}$$

$$\boldsymbol{Z}_r=\frac{B\cdot(E_s/E_r)}{A^2-(E_s/E_r)^2}\overline{/(\phi+\beta)}-$$

$$\frac{A\cdot B}{A^2(E_s/E_r)^2}\underline{/(\beta-\alpha)} \tag{6}$$

This equation 6 is companion to equation 3 and is derived by the same method as given in Appendixes II and III.

Symbols appearing in the preceding equations and elsewhere in this paper are identified in Appendix VIII.

From a relay standpoint, equation 3 is of first interest, because it defines the impedance of a protected circuit as "seen" by relays at the near-end terminal of that circuit. Equation 6 is also of interest but is of less immediate significance, because a diagram plotted from equation 3 gives similar results.

Equation 3 when plotted on impedance co-ordinates is represented by a family of circles as in Figure 9. The mathematical relations between these circles and those of the power circle diagrams, Figure 3, are developed in the appendixes. The derivation of the impedance, equation 3, shows that the center of each circle representing a given ratio of terminal voltages E_r/E_s lies on line 0–0 (Figure 9) passing through the origin ($R=0$, $X=0$) at an angle$\underline{/(\beta-\Delta)}$ with the R–R axis. Five such circles are of particular interest in this discussion. These are the ones corresponding to two general and three special values of the ratio E_r/E_s, as follows:

$$E_s=0,\ \frac{E_r}{E_s}>D,\ \frac{E_r}{E_s}=D,\ \frac{E_r}{E_s}<D,\text{ and }E_r=0$$

The first of these circles which should be examined is one in which the near-end or sending voltage $E_s=0$. In this case ratio $E_r/E_s=\infty$. This corresponds to condition of short circuit at the location of the relays. Since impedance "seen" by the relays is $\boldsymbol{Z}_s=0$, the circle is a point which coincides with origin 0_1.

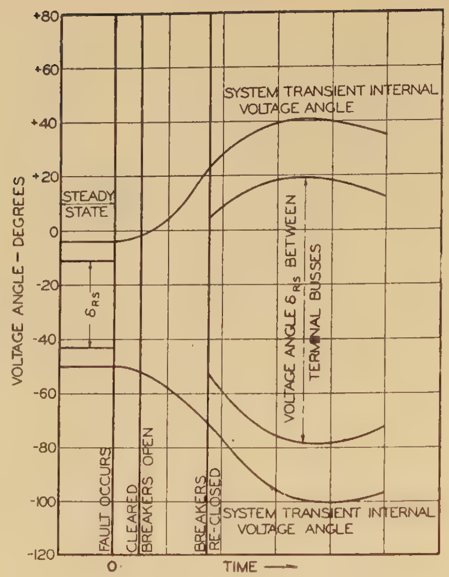


Figure 5. Swing curves of bus voltages and of equivalent internal voltages of generators during tripout and reclosure of interconnecting line

The second circle falls in the general group defined by condition $E_r/E_s>D$. (Referring back to the power circle diagram Figure 3, it is found that this condition defines power circles which enclose the origin.) By plotting one of this general group of circles, it is found that the center lies on the line 0–0 *below* the origin of the diagram, for example at 0_2. One particular case in this group of circles is that for which $E_r=E_s$. In this case, the distance from the center 0_2 to the origin of the diagram is $(D\cdot B)/(D^2-1)$ and the radius is $B/(D^2-1)$.

The third circle of interest is the special case in which $E_r/E_s=D$. In this case (reference to the power circle diagram, Figure 3, reveals that the corresponding power circle passes through the origin of the diagram indicating one point of zero power), the circle has diameter of ∞ and radius of ∞ and is therefore a straight line. This straight line is perpendicular to the line of centers 0–0 and intercepts it at distance $B/2D$ above the origin. However, this location cannot be found from equation 3 but must be found as explained in a later paragraph.

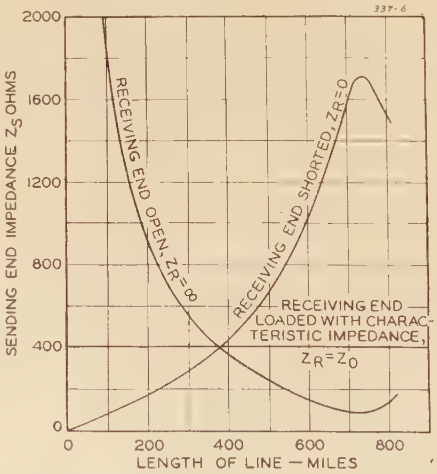


Figure 6. Magnitude of 60-cycle input impedance of a line suitable for 154-kv operation as a function of length and far-end terminal load

The fourth circle is one in the general group defined by the condition $E_r/E_s<D$. (Again referring back to the power circle diagram, Figure 3, it is seen that circles of this group lie entirely to one side of the origin of the diagram.) This fourth circle has finite radius and its center is located on the line of centers 0–0 *above* the origin, for example, at 0_4.

The fifth of these circles is the case where $E_r=0$, corresponding to short circuit at the far end of the line. This circle has no radius and is therefore a point. Its location is on the line of centers 0–0 at distance B/D upward from the origin at 0_5. For convenience this point may be called the far end of the circuit, and the origin 0_1 can likewise be called the near end.

In plotting these circles, the intersection of each with the line of centers 0–0 on the side nearest to the origin 0_1 is of greatest importance. Since these intersections are obtained from the impedance equation 3 as the difference between two quantities which may each be quite large, infinite in one case, it is often desirable to locate these intersections by other more accurate means and then to make the circles pass through these points by slight adjustments in location of the corresponding center or radius if necessary.

Referring back to the power circle diagram, Figure 3, the corresponding point in that diagram is located at M where power circle P_s crosses the line of centers 0–0′ extended. The power P_s at that point has scalar value equal to the *sum* of the radius, and the distance from the origin to the center 0–0′ which sum is $|P_s|_m=(E_s{}^2\cdot D)/B+E_sE_r/B$. The impedance corresponding to $|P_s|_m$ is therefore $E_s{}^2/|P_s|_m$. A numerical substitution of values corresponding to either actual or per-unit voltages permits easy calculation of this impedance which is then used to locate the intersection in question on the impedance circle diagram, Figure 9. It should be noted that all such intersections fall between the points 0_1 and 0_5.

As indicated in the appendixes, the authors have found no satisfactory way of relating the angle ϕ appearing in the impedance equation 3 to the angle δ_{rs} between sending- and receiving-end voltages. To date, the impedance circle diagrams similar to Figure 9 used in their calculations have all had these points

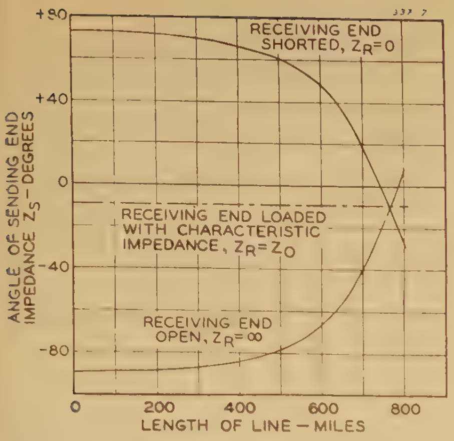


Figure 7. Angle of 60-cycle input impedance of a line suitable for 154-kv operation as a function of length and far-end terminal load

representing various values of δ_{rs} individually computed and located by the following method.

The impedance angle $\psi=\tan^{-1}(X/R)$ appearing on the impedance circle diagram, Figure 5, is the conjugate of the corresponding power-factor angle θ appearing on the power circle diagram. These angles θ are measured for each of several desired values of δ_{rs} on appropriate power circles of diagrams similar to Figure 3 and transferred to the corresponding impedance circle diagrams by locating the intersections of lines Z_s extending from the origin at the conjugate angles ψ and the corresponding impedance circles as in Figure 9.

Locus of Impedance During System Disturbance

If the impedance circle diagram, Figure 9, is used, it is quite enlightening to follow the dynamic behavior of an interconnection during a power swing as it is represented by the swing curves of Figure 5. Prior to system disturbance, the angle δ_{rs} is relatively small, as can be determined from load and terminal voltages by referring to the appropriate power circle diagram similar to Figure 4. Assume for the moment that the direction of the power flow is *from the bus* to the interconnecting circuit at the location of relays. For purposes of this discussion, we may assume the impedance "seen" by the relays to be at A on Figure 9. Upon occurrence of a short circuit on the interconnecting circuit close to the far end, the impedance instantly assumes a value closely corresponding to $E_r/E_s=0$. This may be assumed as being at B.

If relay and breaker action is correct, both ends of the interconnecting circuit are immediately tripped out, and the systems begin to drift apart as indicated in the swing curve, Figure 5. By the time the interconnection has been reclosed at both ends, terminal voltages will have drifted apart by several degrees so that the impedance "seen" by relays has moved around the impedance circle from original position A toward the line of centers 0–0 and now occupies position C, possibly on another circle representing a different ratio E_r/E_s. Further swing increases the angle δ_{rs} until the respective accelerations of the two systems have been overcome and δ_{rs} has reached maximum value.

As has been pointed out earlier in this paper, the angle (δ_{rs}max) may exceed 90 degrees, and the resulting impedances may fall in the general vicinity of D. Recovery of the system toward normal voltages and angles causes the impedance to retrace path D, C, A and oscillate about the original angle and impedance values at A until the systems finally resume their original relation.

A similar step-by-step analysis of behavior based upon the opposite flow of power, that is *from circuit to bus*, is represented by a similar group of points, E, B, F, G, and E, on the left-hand side of the diagram.

Impedance Referred to Far End of a Nonuniform Circuit

Figure 9, which is the plot of equation 3, has been used in the preceding discussion of conditions as "seen" when "looking into" the circuit from the near end or so-called, but somewhat misnamed, "sending end." To complete the study, it is necessary to investigate at the same time what takes place at the far end or receiving end of the circuit. Figure 10 is the plot of equation 6, which presents the same information in terms of far-end or receiving quantities. In comparing these two Figures 9 and 10, the first thing which attracts attention is the fact that the diagrams are similar in construction except for 180-degree rotation with respect to the axis of the diagram. The rotation corresponds to the change in viewpoint required to "see" the circuit from the opposite end. The dimensions of these two diagrams differ from each other only for *nonuniform* circuits in which $D<\Delta$ is not equal to $A<\alpha$. For uniform symmetrical lines, or for such lines terminated in transformer banks having identical electrical characteristics, it would be sufficient to use Figure 9 for far-end or receiving conditions by making the following changes:

(*a*). Rotate the diagram 180 degrees.

(*b*). Reverse the resistance scales, R for $-R$ and vice versa.

(*c*). Reverse the reactance scales, X for $-X$ and vice versa.

(*d*). Substitute reciprocal values for the ratios E_r/E_s.

(*e*). Substitute conjugate values for the angles δ_{rs}; that is, in place of $\delta_{rs}=45$ degrees substitute $\delta_{rs}=315$ degrees, and so forth.

Application of Impedance Circle Diagrams to Long-Line Relaying

The impedance circle diagrams, Figures 9 and 10, can be combined on one chart to give a very complete picture of impedance conditions affecting simultaneous relay performance at the two ends of an interconnecting circuit in the following manner:

1. Cut off each diagram along the straight lines representing the condition $E_r/E_s=D$ in the near-end sending diagram, Figure 9, and the condition $E_s/E_r=A$ in the far-end receiving diagram, Figure 10.

2. Fit the two diagrams together along these two lines and match lines of centers 0–0.

The co-ordinates of the two diagrams are thus offset from each other both horizontally and vertically.

Figure 11 is the result of such combination for the Kansas–Nebraska 154-kv interconnection. Because transformers at the opposite ends of this line are not alike, the A and D constants of the circuit are not equal. Consequently the co-ordinates are skewed slightly, and impedance values scaled across the boundary line L–L are not strictly accurate. In spite of these minor shortcomings, the combined diagrams furnish an excellent basis for relay studies.

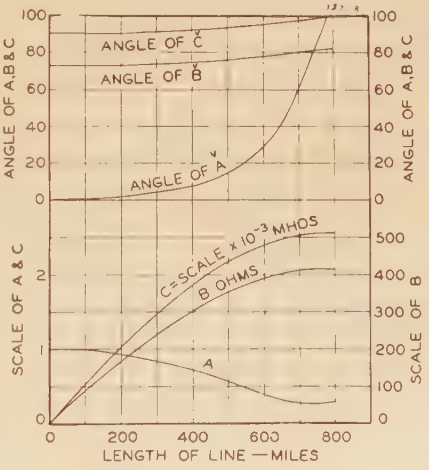


Figure 8. Values of general circuit constants *A*, *B*, and *C* as a function of length for a line having the following constants:

$r=0.252$ ohm per mile
$x=0.803$ ohm per mile
$b=5.22\times10^{-6}$ mho per mile

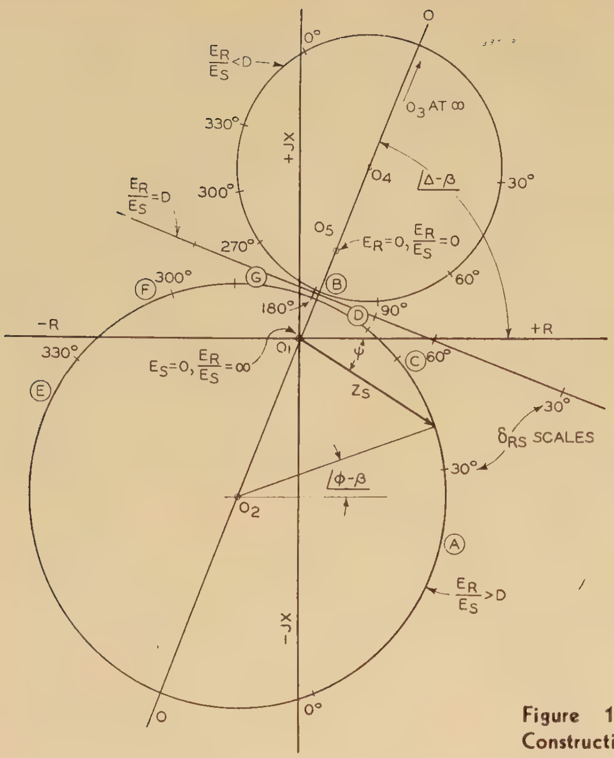


Figure 9 (left). Construction of sending-end impedance circle diagram from equation 3

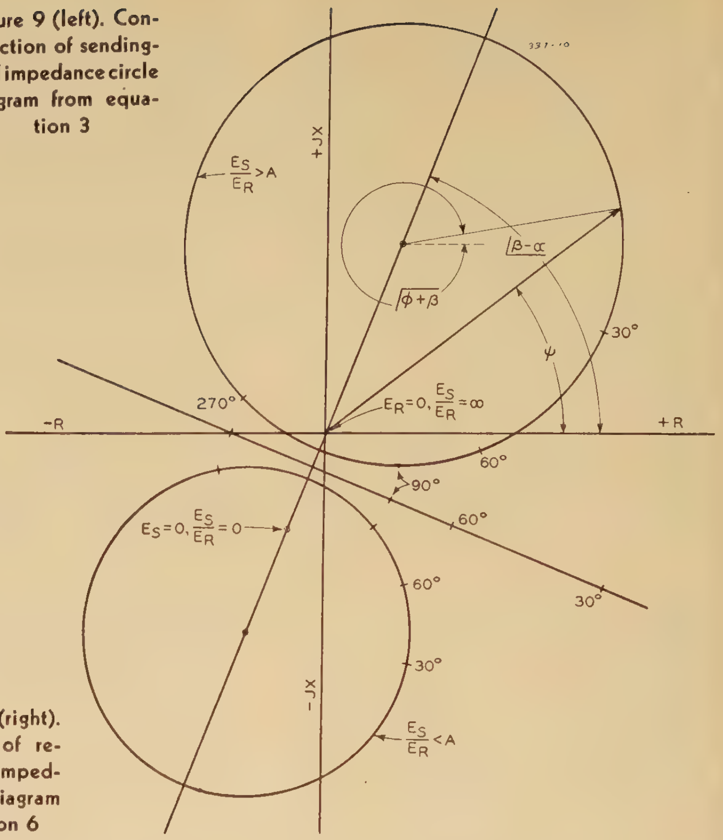


Figure 10 (right). Construction of receiving-end impedance circle diagram from equation 6

It will be noted that for this circuit values of A and D are 0.824 and 0.769 respectively. In no case of steady-state operation is it likely to be desirable or practical to have ratios E_r/E_s or E_s/E_r as low as these A and D values. Therefore it is not necessary to consider seriously impedance circles having centers in the areas which were cut off when these two diagrams were combined. On the combined impedance diagram, Figure 11, circles corresponding to $E_r/E_s = 1.00 \pm 0.10$, or a total range of 20 per cent in voltage ratio, are plotted to serve as approximate boundary lines between which normal operation is expected. Distances along the lines of centers between points K and N are approximate measures of fault location, and the boundaries of trip and no-trip regions are added on this basis.

This combined diagram not only shows the impedance "looking" into the interconnecting circuit from each end, but on it are also plotted limiting impedances which relays will "see" during open-circuit conditions, zero-power-transfer condition, and short-circuit conditions. The lower part of this diagram shows the impedance looking into the Kansas terminal transformers toward Nebraska, and the origin K of the graph corresponds to a short circuit on the Kansas 132-kv bus. Similarly, the upper part of the diagram "looks" into the circuit from the Nebraska 66-kv bus, and short circuit on the Nebraska 66-kv bus falls on the origin N. This combined diagram is replotted in Figure 12.

Analysis of the combined diagram shows that all solid phase-to-phase faults on the line or in the terminal transformers would establish impedances, as "seen" from the ends of the line, which would fall along the line of centers 0–0 between the origins or terminals N and K. If the fault is an arcing type such as one started by lightning, the fault impedance as "seen" from terminal K would fall in the shaded area B in Figure 12 to the right of the line K–N because of the arc resistance.

Similarly, for the same arcing fault, the impedance "seen" from terminal N would fall in the shaded area H to the left of the line K–N.

Normal operating impedances correspond to the shaded areas A and A' for power flow from K to N and to the areas E and E' for power flow from N to K. The exact impedances in these general areas are determined by the ratio E_r/E_s and the angle δ_{rs}. Both of these factors can be determined for any load condition from the power circle diagrams of the circuit.

During system disturbances, the impedances "seen" by the relays may move out of the normal operating areas A and A', or E and E', into the regions of maximum synchronizing power D and D', or G and G', as explained in a previous section "Locus of Impedance During System Disturbance." If the systems do not recover from the disturbance and thus fall out of step, this loss of synchronism is indicated by further movement of the impedance across the line N–K and through the areas corresponding to faults near the middle of the circuit.

Impedance magnitudes at the high-load edge of areas A, A', E, E' and in the areas of maximum synchronizing power are less than some impedance magnitudes in the fault areas B and H.

This clearly requires that a closely drawn border line must be established between the power-swing area, from which the systems may be expected to recover, and the actual fault zone. To obtain the maximum usefulness of an interconnection, it is necessary for the relays controlling the circuit to discriminate accurately between trip and no-trip conditions along this border line. On interconnections of this nature, there is no harm in permitting tripping on the out-of-step swing into the fault area after all hope of maintaining synchronism is lost. In fact, tripping before the angle δ_{rs} first reaches 180 degrees rather than waiting through two or three cycles of slip to recognize the out-of-step condition is of utmost importance to prevent propagation of further system disturbances.

SUMMARY

The characteristics of long lines and interconnections differ from those of short radial feeders and loops within close-knit networks and dictate special relaying treatment for these circuits. These characteristics may be expressed in terms of impedance magnitude and angle on diagrams similar to Figure 9 and Figure 11. If these impedance diagrams are used, it is possible to define completely and clearly the necessary relay requirements to protect and control a long power-transmission circuit or interconnection.

Required Performance of Long-Line Relays

Expressed in the language of these diagrams, relays for long power-transmission lines or for loose-linked interconnections should:

1. Close their trip-circuit contacts instantaneously at both terminals for all impedance values occurring between terminals *N* and *K* in areas *B* and *H* of Figure 12 caused by either power swings or faults.

2. Not trip at any time for values of circuit impedance corresponding to high-power swings or high kilovolt-ampere transfers in the areas *A*, *A'*, *D*, *D'*, or *E*, *E'*, *G*, *G'*.

3. Provide time-delay backup tripping for faults in areas *B* and *H* which are beyond the far end of the circuit.

4. Prevent immediate tripping of terminal breakers, for faults in the systems external to the protected circuit, by means of carrier-current blocking principles. There should be no objection to such tripping after proper time delay.

5. Provide instantaneous tripping of terminal breakers for ground faults on the protected circuit by means of carrier-current-controlled directional ground relays.

6. Provide time-delay backup protection independent of carrier-current control for all ground faults.

LIMITATIONS OF LONG-LINE PROTECTIVE RELAYS

From a very brief study of lines longer than the 270-mile interconnection cited in this paper, it appears that these same principles of protective relaying can be applied to transmission lines of greater length without intermediate switching up to any limit at which synchronism can be maintained between constant voltage systems. This limitation is imposed by the fact that the input-impedance angles $\psi = \tan^{-1} X/R$ for such long lines approach the characteristic impedance angle which is small.

In considering the field of unusually long-distance transmission of power, it appears that but little consideration has yet been given to methods of protecting the lines to differentiate between faults and power swings. It is hoped that this analysis of relay performance requirements will stimulate thought and discussion on this complex subject.

Appendix I. Power-Circle-Diagram Equations 1 and 4

The general *ABCD* equations for a circuit are

$$\boldsymbol{E}_s = \boldsymbol{A} \cdot \boldsymbol{E}_r + \boldsymbol{B} \cdot \boldsymbol{I}_r \tag{7}$$

$$\boldsymbol{I}_s = \boldsymbol{C} \cdot \boldsymbol{E}_r + \boldsymbol{D} \cdot \boldsymbol{I}_r \tag{8}$$

From equation 7, $\boldsymbol{I}_r = (\boldsymbol{E}_s/\boldsymbol{B}) - (\boldsymbol{A} \cdot \boldsymbol{E}_r/\boldsymbol{B}.)$

Substituting this into equation 8, we have

$$\boldsymbol{I}_s = \boldsymbol{C} \cdot \boldsymbol{E}_r + \boldsymbol{D} \cdot \left(\frac{\boldsymbol{E}_s}{\boldsymbol{B}} - \frac{\cdot \boldsymbol{E}_r \boldsymbol{A}}{\boldsymbol{B}} \right)$$

$$= \frac{\boldsymbol{D} \cdot \boldsymbol{E}_s}{\boldsymbol{B}} + \boldsymbol{E}_r \left(\boldsymbol{C} - \frac{\boldsymbol{A} \cdot \boldsymbol{D}}{\boldsymbol{B}} \right) \tag{9}$$

$$\boldsymbol{I}_s = \frac{\boldsymbol{D} \cdot \boldsymbol{E}_s}{\boldsymbol{B}} + \frac{\boldsymbol{E}_r}{\boldsymbol{B}} \cdot (\boldsymbol{B} \cdot \boldsymbol{C} - \boldsymbol{A} \cdot \boldsymbol{D})$$

Since $\boldsymbol{A} \cdot \boldsymbol{D} - \boldsymbol{B} \cdot \boldsymbol{C} = 1$

$$\boldsymbol{I}_s = \frac{\boldsymbol{D} \cdot \boldsymbol{E}_s}{\boldsymbol{B}} - \frac{\boldsymbol{E}_r}{\boldsymbol{B}} \tag{10}$$

Using equation 10 and substituting for $\boldsymbol{I}_s$ in

$$\boldsymbol{P}_s = \sqrt{3}\ (\text{conj } \boldsymbol{E}_s) \cdot \boldsymbol{I}_s = P \pm jQ$$

$$\boldsymbol{P}_s = \frac{(\text{conj } \boldsymbol{E}_s) \cdot \boldsymbol{D} \cdot \boldsymbol{E}_s}{\boldsymbol{B}} - \frac{(\text{conj } \boldsymbol{E}_s) \cdot \boldsymbol{E}_r}{\boldsymbol{B}} \tag{11}$$

If δ_{rs} represents the variable angle between terminal bus voltages, this equation may be expressed as follows (writing the circuit constants in polar form):

$$\boldsymbol{P}_s = \frac{E_s^2 \cdot D/\Delta}{B/\underline{\beta}} - \frac{E_s \cdot E_r/\delta_{rs}}{B/\underline{\beta}} \tag{1}$$

or

$$\boldsymbol{P}_s = \frac{E_s^2 \cdot D}{B} \underline{/(\Delta - \beta)} - \frac{F_s \cdot E_r}{B} \overline{/(\delta_{rs} + \beta)} \tag{1}$$

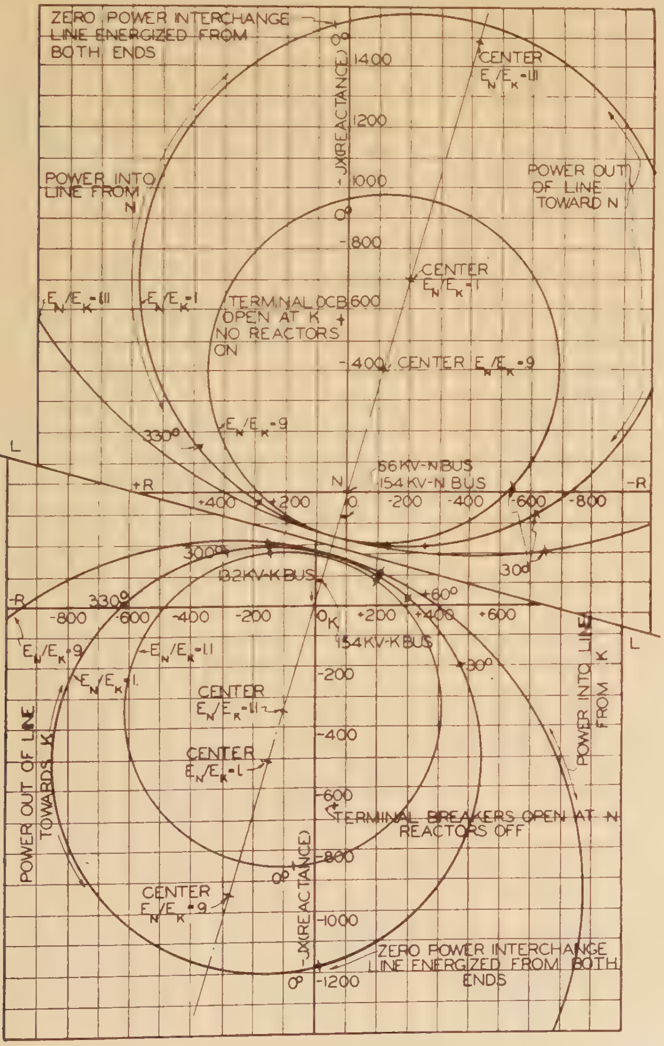
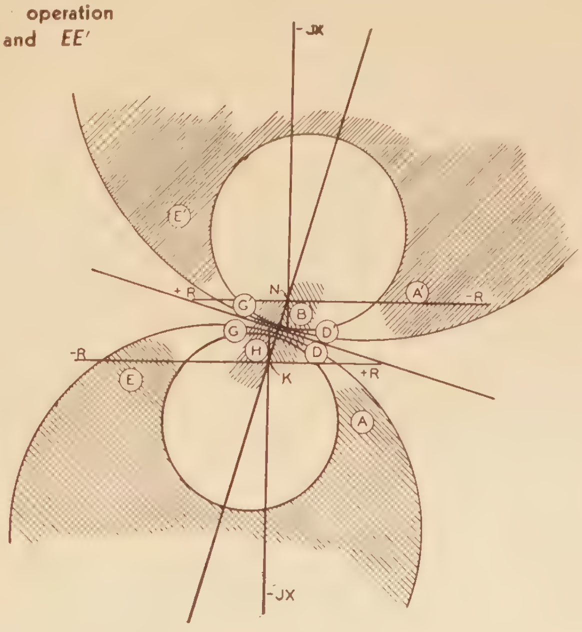


Figure 11 (left). Combined sending-end and receiving-end impedance circle diagrams for Kansas–Nebraska 154-kv interconnection including transformers at both ends

Figure 12 (below). Same as Figure 11 but showing areas of fault *BH*, swing *DD'* and *GG'*, and normal operation *AA'* and *EE'*



Similarly, solving for $\boldsymbol{E}_r$ and $\boldsymbol{I}_r$ from equations 7 and 8, equation 4 for receiving power may be obtained.

$$\boldsymbol{P}_r=\frac{E_s\cdot E_r}{B}\underline{/(\delta_{rs}-\beta)}-\frac{E_r{}^2\cdot A}{B}\overline{/(\beta-\alpha)} \qquad (4)$$

In equations 7, 8, 9, and 10, voltages, currents, and circuit constants are on phase-to-neutral basis. In equations 11, 1, and 4 power is total three-phase power with voltages on a phase-to-phase basis, currents are phase currents, and circuit constants are on phase-to-neutral basis.

Appendix II. Basic Impedance Circle Diagram Equation

In order to derive equations 3 and 6, the following review is presented covering the method of inverting a complex quantity of the form:

$$\boldsymbol{Y}_s=a\underline{/\alpha}-b\overline{/(\delta-\beta)} \text{ (see reference 8)} \qquad (12)$$

This equation is an admittance form which can be converted to the impedance form using graphical resolution and some artifices.

A plot of admittance, equation 12, is shown in Figure 13. $a\,\underline{\angle\alpha}$ locates the center of the circle, and $b\overline{/(\delta-\beta)}$ is the rotating vector with variable angle δ. Principal points in this figure are labelled 1, 2, and 3.

If the reciprocals of points on this circle are taken and plotted on a convenient scale, another circle will result as shown in Figure 14, which is the locus of the end of the impedance vector $\boldsymbol{Z}_s$ (the reciprocal of $\boldsymbol{Y}_s$), and points on it are conjugate impedance values from Figure 13. An equation may be derived for Figure 14 by first comparing points along the complex quantity passing through the center of the circle with corresponding points in Figure 13. If Y_{o1} is the distance from the origin to point 1, then, if the values from equation 12 for the distance to the center of the circle and radius of circle are used, $Y_{o1}=a-b$ and $Y_{o2}=a+b$.

The corresponding points in the impedance diagram Figure 14 are

$$Z_{o1}=\frac{1}{a-b},\quad Z_{o2}=\frac{1}{a+b}$$

Diameter of impedance circle in Figure 14 is

$$Z_{o1}-Z_{o2}=\frac{1}{a-b}-\frac{1}{a+b}=\frac{2b}{a^2-b^2}$$

and radius is

$$R=\frac{\text{diameter}}{2}=\frac{Z_{o1}-Z_{o2}}{2}=\frac{b}{a^2-b^2}$$

The magnitude of the complex quantity locating the center of the impedance circle is

$$(Z_{o2})+(\text{radius, } R)=\frac{1}{a+b}+\frac{b}{a^2-b^2}=\frac{a}{a^2-b^2}$$

By analysis and comparison the angles may be found, and hence complete impedance equation for Figure 14 is

$$\boldsymbol{Z}=\frac{a}{a^2-b^2}\overline{/\alpha}-\frac{b}{a^2-b^2}\underline{/(\phi-\beta)} \qquad (13)$$

It is interesting to note how the preceding equation 13 is related to the admittance equation 12. The constants a and b are now divided by (a^2-b^2). Also the conjugate of $\angle\alpha$ and $\underline{/(\delta-\beta)}$ are the angles of the fixed quantity and radius of the circle respectively. But angle δ is not the same as ϕ as discussed later in the following appendix.

Appendix III. Equation 3

The derivation of this impedance equation is based on the following fundamental equations:

$$\boldsymbol{Z}_s=\frac{(\text{conj } \boldsymbol{E}_s)\cdot\boldsymbol{E}_s}{\boldsymbol{P}_s}=\frac{E_s{}^2}{\boldsymbol{P}_s} \qquad (2)$$

$$\boldsymbol{P}_s=\frac{E_s{}^2\cdot D}{B}\underline{/(\Delta-\beta)}-\frac{E_s\cdot E_r}{B}\overline{/(\delta_{rs}+\beta)} \qquad (1)$$

Equation 2 shows the relation between the three-phase power in any part of a circuit, the phase-to-phase voltage of that part of the circuit, and the phase-to-neutral impedance. Equation 1 is derived in Appendix I.

The reciprocal of equation 2 is the admittance form as follows:

$$\boldsymbol{Y}_s=\frac{1}{\boldsymbol{Z}_s}=\frac{\boldsymbol{P}_s}{E_s{}^2} \qquad (14)$$

Substituting equation 1 into the preceding equation gives the following general equation:

$$\boldsymbol{Y}_s=\frac{1}{Z_s}=\frac{\dfrac{E_s{}^2\cdot D}{B}\underline{/(\Delta-\beta)}-\dfrac{E_r\cdot E_s}{B}\overline{/(\delta_{rs}+\beta)}}{E_s{}^2} \qquad (15)$$

on simplifying

$$\boldsymbol{Y}_s=\frac{1}{Z_s}=\frac{D}{B}\underline{/(\Delta-\beta)}-\frac{E_r}{E_s\cdot B}\overline{/(\delta_{rs}+\beta)} \qquad (16)$$

Since this is an admittance equation, it is not wholly satisfactory for our purpose if we discuss impedance relays and their relation to circuit impedance. This admittance form will, however, be very useful as will be shown later. By taking the reciprocal of this equation, as follows:

$$\boldsymbol{Z}_s=\frac{1}{\boldsymbol{Y}_s}=\frac{1}{\dfrac{D}{B}\underline{/(\Delta-\beta)}-\dfrac{E_r}{E_s\cdot B}\overline{/(\delta_{rs}+\beta)}} \qquad (17)$$

we get an equation for impedance in terms of circuit constants, circuit voltage angle, and ratio of receiving and sending voltages, but it is not in a form for conveniently plotting.

By going through some algebra, equation 17 may be written in the following form:

$$\boldsymbol{Z}_s=\frac{(b_1a_1+b_2a_2)+(b_2\sin\delta_{rs}-b_1\cos\delta_{rs})}{1+|A|^2-2a_1\cos\delta_{rs}-2a_2\sin\delta_{rs}}+j\frac{(b_2a_1-b_1a_2)+(b_1\sin\delta_{rs}-b_2\cos\delta_{rs})}{1+|A|^2-2a_1\cos\delta_{rs}-2a_2\sin\delta_{rs}} \qquad (18)$$

which also is not conveniently usable because of the large amount of arithmetical work required to plot the diagram representing it.

Instead of directly inverting equation 16, inverting an admittance equation to an impedance form as in Appendix II simplifies greatly the amount of work required and is a method commonly used. Admittance equation 16 was converted to the following impedance equation in this way:

$$\boldsymbol{Z}_s=\frac{D\cdot B}{D^2-\left(\dfrac{E_r}{E_s}\right)^2}\overline{/(\Delta-\beta)}-\frac{B\cdot\left(\dfrac{E_r}{E_s}\right)}{D^2-\left(\dfrac{E_r}{E_s}\right)^2}\underline{/(\phi-\beta)} \qquad (3)$$

This equation for impedance includes the general circuit constants, angles of these constants, ratio of the receiving and sending voltages, and the angle ϕ. The diagram for this equation is Figure 9 discussed in the text of the paper.

Again referring to equation 16, if $E_s=E_r$, then this equation takes the following simple form:

$$Y_s=\frac{D}{B}\underline{/(\Delta-\beta)}-\frac{1}{B}\overline{/(\delta_{rs}+\beta)} \qquad (19)$$

which is the sending-end power, equation 1, with the sending and receiving voltages equal to one volt. Thus the admittance is expressed in terms of the circuit constants and circuit angle, and it does not include the load impedance. On analyzing this equation, we find it is a circle with the center at $D/B/(\Delta-\beta)$ and a radius of $1/B$. The angle δ_{rs} is the same as the circuit voltage angle in the preceding power equation 1. This operation shows that the admittance value of a network or transmission line may be obtained from the sending-end power circle diagram for conditions where E_r equals E_s by simply dividing the power and reactive values by $E_s{}^2$. The reciprocal of this, of course, gives the impedance. It is interesting to note that Y_s is not a function of the magnitude of the line voltage, but only of the ratio of the sending and receiving voltages. Thus for a given transmission line, equation 19 gives the sending-end admittance, regardless of the magnitude of the line voltage.

In reference to equation 3, if E_r equal E_s, then the following simple equation for impedance results:

$$Z_s=\frac{D\cdot B}{D^2-1}\overline{/(\Delta-\beta)}-\frac{B}{D^2-1}\underline{/(\phi-\beta)} \qquad (20)$$

As mentioned in the text of this paper, the angle ϕ in equations 3 and 20 is not the same as the angle δ_{rs} in power equations 1 and 4. They are related for the particular problem studied by a curve similar to the Figure 15 for which a simple equation has not been found.

Appendix IV. Equation 6

In a manner similar to that in Appendix III, an equation for receiving-end impedance

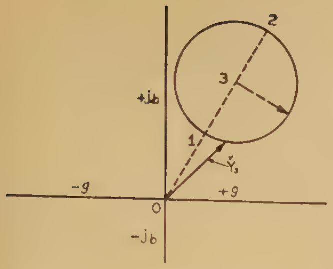


Figure 13. Admittance circle—Y_s

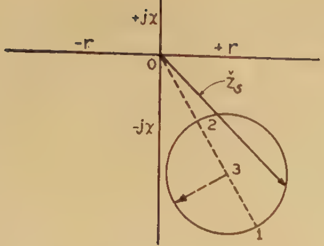


Figure 14. Impedance circle—Z_s derived from Figure 13

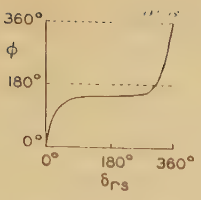


Figure 15. Variation of the angle ϕ with respect to the angle δ_{rs}

may be derived using the following fundamental formula for impedance:

$$Z_r=\frac{(\text{conj } E_r)\cdot E_r}{P_r}=\frac{E_r^2}{P_r} \qquad (5)$$

and the following equation for receiving-end power derived in Appendix I:

$$P_r=\frac{E_s\cdot E_r}{B}\underline{/(\delta_{rs}-\beta)}-\frac{E_r^2\cdot A}{B}\overline{/(\beta-\alpha)} \qquad (4)$$

we obtain the following equation for receiving-end impedance:

$$Z_r=\frac{B\cdot\left(\frac{E_s}{E_r}\right)}{A^2-\left(\frac{E_s}{E_r}\right)^2}\overline{/(\phi+\beta)}-\frac{A\cdot B}{A^2-\left(\frac{E_s}{E_r}\right)^2}\underline{/(\beta-\alpha)} \qquad (6)$$

This equation may be plotted in the same manner used to plot equation 3 for sending-end impedance circle. Again if $E_r=E_s$, then the above reduces to

$$Z_r=\frac{B}{A^2-1}\overline{/(\phi+\beta)}-\frac{A\cdot B}{A^2-1}\overline{/(\beta-\alpha)} \qquad (21)$$

Similarly to equation 16

$$Y_r=\frac{E_s}{E_r\cdot B}\underline{/(\delta_{rs}-\beta)}-\frac{A}{B}\cdot\overline{/(\beta-\alpha)} \qquad (22)$$

Appendix V. Plotting Impedance Circle Diagrams

Impedance circle diagram in Figure 9 was plotted by first calculating the vector which locates the center of the circles and the radii of the circles, using the following equation for impedance:

$$Z_s=\frac{D\cdot B}{D^2-\left(\frac{E_r}{E_s}\right)^2}\overline{/(\Delta-\beta)}-\frac{B\cdot\left(\frac{E_r}{E_s}\right)}{D^2-\left(\frac{E_r}{E_s}\right)^2}\underline{/(\phi-\beta)} \qquad (3)$$

Since the angle ϕ does not bear a simple relation to δ_{rs}, the degree points on the diagrams corresponding to values of δ_{rs} were obtained by taking the reciprocal of the admittance quantity obtained from equation 16 after ratios of approximate system voltages and circuit constants had been substituted into it.

Similarly equation 6 was plotted and degree points for δ_{rs} were located by using equation 22 to find the admittance for a given value of δ_{rs}.

Appendix VI. General Circuit Constants

General circuit constants used in the impedance, admittance, and power equations for the Kansas–Nebraska 154-kv interconnection including transformers are

$$A=A\underline{/\alpha}=a_1+jb_2=0.824\underline{/3.25°}$$

$$B=B\underline{/\beta}=b_1+jb_2=282\underline{/77.3°}\text{ ohms}$$

$$C=C\underline{/c}=c_1+jc_2=1{,}337\times10^{-6}\underline{/90.9°}\text{ mhos}$$

$$D=D\underline{/\Delta}=d_1+jd_2=0.769\underline{/3.56°}$$

Appendix VII. Auxiliary Formulas

Using the general $ABCD$ circuit equations 7 and 8, Appendix I, auxiliary formulas were derived for calculating limiting circuit impedances.

Sending-end impedance as function of load impedance is

$$Z_s=\frac{A\cdot Z_r+B}{C\cdot Z_r+D} \qquad (23)$$

Assume short circuit at R end; that is, $E_r=0$, then

$$Z_s=\frac{B}{D} \qquad (24)$$

with the circuit open at R end, that is, $I_r=0$, then

$$Z_s=\frac{A}{C} \qquad (25)$$

The same general equations 7 and 8 and same reference angles being used, the receiving impedance Z_r in terms of the sending impedance Z_s is

$$Z_r=\frac{D\cdot Z_s-B}{-C\cdot Z_s+A} \qquad (26)$$

Assume short circuit at S end; that is $E_s=0$, then

$$Z_r=-\frac{B}{A} \qquad (27)$$

With S end open; that is $I_s=0$, then

$$Z_r=-\frac{D}{C} \qquad (28)$$

Appendix VIII. Nomenclature

Z_s—Equivalent or apparent phase-to-neutral impedance in ohms looking into the sending end assumed for purpose of establishing a convention for signs and reference angles during calculations.

Z_r—Equivalent or apparent phase-to-neutral impedance in ohms at receiving end.

Z—Impedance expressed in vector form.

Y_s, Y_r—Equivalent or apparent phase-to-neutral admittances in mhos.

ψ—$\text{Tan}^{-1}\frac{X}{R}$ of impedance Z.

ϕ—The angle of the variable in equations 3 and 6.

δ_{rs}—Angle in degrees by which the sending-end voltage leads the receiving voltage.

A, B, C, D—General circuit constants expressed in vector form.

P_s—Sending-end power expressed as a vector $=P_s\pm jQ_s=P_s\underline{/\theta}$.

P_r—Receiving-end power expressed as a vector $=P_r\pm jQ_r=P_r\underline{/\theta}$.

θ—Power-factor angle of sending or receiving power.

E_s—Phase-to-phase volts at sending end. Phase-to-neutral volts in equations 7, 8, 9, and 10.

E_r—Phase-to-phase volts at receiving end. Phase-to-neutral volts in equations 7, 8, 9, and 10.

E_s, E_r—Vector forms of E_s and E_r, respectively

(conj E_s), (conj E_r)—Conjugate vector forms of E_s and E_r, respectively, that is, with angular rotation measured in *opposite* direction from reference line compared to direction of measurement for E_s and E_r.

(conj E_s)$\cdot E_s=E_s^2$.

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Measurements Pertaining to the Co-ordination of Radio Reception With Power Apparatus and Systems

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I. Introduction

EXPERIENCE has shown that electric-power apparatus and circuits may, under certain circumstances, influence radio reception. Groups* representing electric-apparatus manufacturers, power companies, radio-apparatus manufacturers and service have been co-operating for several years on the control of this influence. Reports by these groups have summarized progress as this work proceeded. Standard measurement equipments and methods have been established. Papers have been presented also covering various aspects of the general problem. This paper gives the results of practical experience with standard equipments and methods for the measurement of radio influence factors. The various elements in the chain between measured characteristics of the power apparatus and the noise measured in the radio set are analyzed. Quantitative values for the various factors involved in average cases are given.

Problems attendant to interference control by general consent are approached with the attitude that we must have the services of both power and radio and that each must adjust itself to live with the other on the practical basis of general usefulness and reasonable economy. Power service groups have made progress in controlling interference phenomena in their circuits. In the radio field improved reception has been realized. Each group is continuing its endeavors to improve its own facilities. They are also working together co-operatively to understand the problems involved and by joint effort to arrive at the most satisfactory over-all solution.

II. General Situation

The general scope of the co-ordination problem ranges widely through many branches of the power and radio fields, and accordingly a comprehensive plan of attack might appear difficult. However, the general situation has naturally divided itself into four parts, each one of which falls automatically into the hands of a technical group whose regular interests prompt ready response to the attendant problems.

These divisions are as follows:

1. Power circuit generation, transmission and distribution and power equipment, including utilities, industrial and domestic classifications. These constitute the generating sources of the influence phenomena and the circuits which transmit them to points of exposure to the radio circuits.
2. The radio receivers and circuits which may be susceptible to the influence of the power apparatus.
3. Magnetic or electrostatic coupling between power circuits of 1 and radio circuits of 2.
4. Measuring instruments which are suitable for the quantitative evaluation of currents and voltages associated with interference or interference conditions.

The technical problems of the first division fall naturally to the electric-power group; those of the second to the radio group; those of the third to co-operative effort between the power and radio groups, and the fourth or instrument division to instrument technicians of both groups.

III. The Immediate Problem

Within this situation, as already outlined, the immediate practical problem of increasing control and reducing interference is that of recognizing the factors involved, determining their significant relations and evaluating them quantitatively. Experience seems to indicate that the general problem is concerned with the following factors and particular problems:

1. The design, construction, and calibration of instruments suitable for the measurement of the electrical characteristics of radio-influence phenomena.
2. The measurement of radio-influence characteristics of power apparatus of all types to indicate the relative influence possibilities when placed in service.
3. The measurement of radio-influence voltage on power lines to establish a relation between the value existing on the line and the measured characteristics of the apparatus connected to it as found in item 2.
4. The measurement of radio-influence voltage under the power line to establish the relation between the radio-influence voltage at some standard position adjacent to the line but not connected to it and the radio-influence voltage on the line as measured in item 3.
5. The measurement of radio-noise voltage on the receiver antenna to determine the relation between its value and the radio-influence voltage measured at the standard position under the power line in item 4.
6. The measurement of the susceptibility of the radio receivers and antennas to extraneous influences.
7. The determination of limiting values for the ratio of signal to noise for good reception.
8. The determination of limiting program signal strengths on which to base a criterion of good reception.
9. The design of suitable equipment or arrangements for the reduction of radio-noise phenomena where prevailing levels are above those consistent with good reception.

This paper will take up these factors and problems and endeavor to present pertinent data and show the present status of each.

IV. Radio-Noise Meters and Calibrations

Satisfactory instruments for the measurement of radio-interference phenomena have certain unusual requirements:

1. They must measure a standardized quantity significant to the noise output of broadcast receivers.
2. They must be extremely sensitive. Voltages as low as a few microvolts (millionths of a volt) must be measured.
3. They must cover a wide range of wave shapes and equivalent frequencies. Radio-interference phenomena vary from single-polarity impulses or damped oscillations of widely different recurrent times to continuous waves of constant frequency. Amplitudes also vary quite irregularly.

Progress in instrument design and in methods of operation have been made, and these developments have been reported

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* American Standards Association committee on radio electrical co-ordination; Joint Co-ordination Committee on Radio Reception of Edison Electric Institute, National Electrical Manufacturers Association and Radio Manufacturers Association; National Electrical Manufacturers Association committee on co-ordination of acoustic relations of power and communication apparatus and systems.

as fast as experience justified.[1-6] For some time now, two makes of radio-noise meters have been available commercially, and considerable experience has accumulated as to their operational performance. To summarize this experience briefly, some results on sine-wave calibration, square-wave calibration, and noise tests on several devices are reported here.

As regards instrument calibrations, both instruments are supplied with a self-contained calibrating signal source which utilizes the electron shot noise of a saturated diode as the calibrating voltage. Operating instructions on calibration call for the following:

1. Tune the noise meter to the desired frequency.
2. Adjust the calibration circuit to the reference point on the noise-meter scale.
3. Adjust the noise-meter amplification until the meter reads the scale value on the calibration curve for the frequency selected.

This calibration procedure has been followed closely, and, in addition, at frequent intervals and before each set of important noise measurements, a check calibration against a signal generator has been made, following the settings called for on the internal calibrator. Many of these complete calibrations have been on both instruments, and rather complete data are, therefore, now available. The general trend of these data is represented by the specimen curves of Figure 1. These were all made at 1,000 kilocycles.

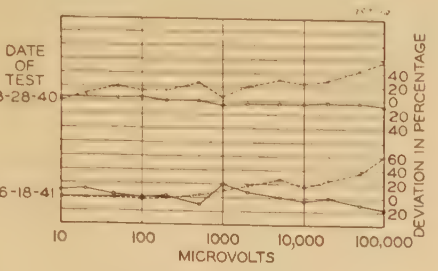
These data show that the accuracy provided by the use of the internal calibrator alone as recommended will be within ±20 per cent for the microvolt range of 10 to 100. For the higher range 100 to 100,000 microvolts, the deviation occasionally goes above ±50 per cent, particularly at the upper end of the measurement range. For any one instrument calibration a high or low trend persisting throughout the measurement range is quite evident. This trend results in lower accuracy for the higher magnitudes of microvolt levels.

Table I. Comparison of Noise Meters on Square Waves

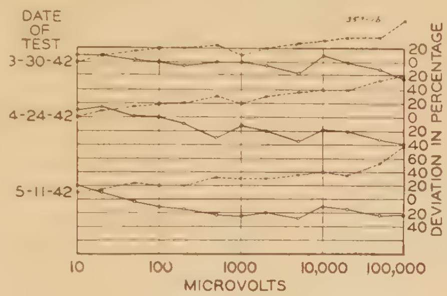
Square-Wave Reference Setting	Noise-Meter Measurements		Per Cent Deviation Based on B
	Type A	Type B	
0.14	58	45	+29
0.22	105	75	+40
0.38	210	150	+40
0.52	310	220	+41
0.65	370	290	+34
0.78	480	350	+37
0.90	580	410	+41
1.05	670	500	+34
		Average	+37

Each instrument has a tendency to read high or low on an average basis, and each one may vary from one calibration to another. This calibration experience is presented as representative of that obtained in a laboratory where operators are familiar technically with the details of such measurements.

If greater accuracy is desired, the radio-noise meter may be used by connecting it to the circuit in which the noise is to be measured and noting the reading. It is then connected to the signal generator and the output adjusted until the same reading is obtained on the radio-noise meter. The output of the signal generator then gives the radio-influence voltage in microvolts. Such a method of measurement is slower but results in accuracies within eight per cent.



(a). Tests of 8-28-40 and 6-18-41



(b). Tests of 3-30-42, 4-24-42, and 5-11-42

Figure 1. Radio-noise-meter calibrations

Solid lines all apply to an instrument of one make. Dotted lines all apply to an instrument of another make

The preceding comparison has been made on the basis of a sine wave of radio frequency produced by a signal generator. As the radio-influence voltage measured on electric apparatus is not usually sinusoidal in character but may consist of a very irregular frequency spectrum, it is desirable to have some idea of the behavior of the instruments on other types of waves. Since square-wave signal generators are available, it was decided to compare the performance of the radio-noise meters on this type of wave. An interesting specimen measurement wherein the two noise-meter types were compared for response is summarized in Table I.

These measurements were made with the two meters connected in parallel on the square-wave generator following calibrations of each instrument on a sine-wave signal generator at 1,000 kilocycles. A definite difference between the two instruments is indicated by these data over a wide range of voltage magnitudes. Type *A* reads on an average 37 per cent higher than type *B*. The obvious conclusion is that setting on the internal calibrator supplemented by sine-wave calibration may nevertheless result in considerable differences in readings for two instruments on different wave shapes. In view of the wide range of wave shapes and equivalent frequencies common to radio-noise phenomena, this circumstance shows a definite need for further work to reduce these differences.

As regards comparisons between the two types of meters on measurement of radio-influence voltage of power apparatus, both high and low voltage, data of Table II are interesting. These comparative tests were made following a sine-wave calibration on the signal generator. Measurements were made in each case with the two instruments connected in parallel.

As these data are backed up by sine-wave calibrations and were obtained with the two types of instruments connected in parallel and measured at the same time, they show directly a rather widely different instrument response. This difference in per cent ranges up to 35 per cent and persists over a wide range of noise-voltage magnitudes without any evident trend as to magnitude.

Both types of instruments used in these tests are arranged by their designers to be

Table II. Comparison of Noise Meters on Power Apparatus

Test No.	Test Voltage (60 Cycles) Kilovolts	Radio-Influence Voltage (Microvolts) Instruments		Per Cent Deviation Based on Type B Instrument
		Type A	Type B	
1 Insulator	10	480	450	+ 7.0
	16	2,600	2,600	0
	22	4,200	4,900	−14.0
	28	5,200	6,000	−13.0
2 Insulator	10	2,100	1,900	+10.0
	16	7,900	8,200	− 3.0
	22	13,000	16,000	−19.0
	28	20,000	23,000	−13.0
3 Small gap	14	23,000	17,000	+35.0
4	0.115	290	250	+16.0
1st small motor	0.115	1,600	1,200	+33.0
5	0.115	1,300	1,600	−19.0
2nd small motor	0.115	1,900	1,750	+ 8.0

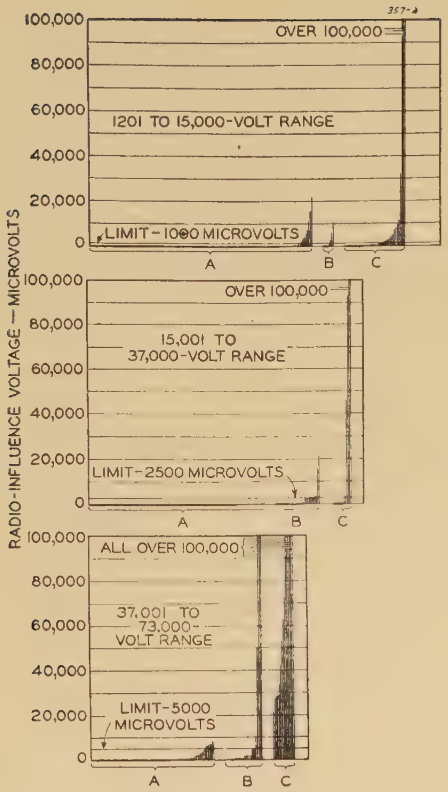


Figure 2. Summary charts of radio-influence voltage data on high-voltage apparatus

For 1,201- to 15,000-volt range each vertical line or dot represents five tests. For all other ranges each vertical line or dot represents one test

A—New apparatus
B—In service without complaints of radio noise
C—Removed because of radio noise and complaints

in general accord with the specifications.[5] Some investigational work has been directed toward the evaluation of instrument characteristics, such as detector-circuit time constants, to ascertain how variations in these might contribute toward measurement differences. To date this has not resulted in improved measurement accuracies.

Consideration of the calibration problem usually proceeds along the lines of specifying instrument-design details or specifying certain calibration waves. The present specifications cover only such over-all design features as seemed necessary to the duplication of radio-receiver characteristics pertinent to noise levels and the incorporation of good instrumentation qualities. Specific details of circuits were not recommended and to the designer was left the freedom of selecting these and improving them when progress permitted. This attitude insures a situation in which advances in design are stimulated.

However, from the above data which are believed to be generally representative, it is obvious that further specifications are necessary. In view of the indicated importance of wave shape, it is desirable that steps be taken toward the specification of calibrating waves of representative shape and equivalent frequency. This has been suggested by one of the authors,[7] and proposals along similar lines have been made by others.

The sine wave now occupies an important place in calibration, is readily obtained, and should be retained. To it might be added a square wave of variable duration and frequency. The wave front should be the maximum obtainable on a practical technical and economic basis. This wave should be available in either polarities to ground.

For investigational purposes in arriving at the necessary wave shapes, a high-frequency damped oscillation of constant decrement and variable frequency of occurrence might be tried. The waves might be examined by recording means such as a cathode-ray oscillograph. Suitable oscillographs of high-writing speed are now available.[8,9]

The use of recommended calibrating-wave shapes would provide for noise-meter calibration or check immediately at the place of noise measurement, or at least at one or more reference places.

It seems possible that the circuits for such calibrating-wave shapes might be standardized in sufficient detail (even to the length of leads, and so forth), so that the wave shapes would be very closely alike. Thus a certain degree of standardization could be accomplished as to wave shape while noise-meter design problems are left entirely in the hands of the instrument designers where they are handled most capably.

To use the radio-noise meter for measurement of radio-influence characteristics of apparatus, it is necessary in order to get comparable results to standardize on setup for making tests. A general recommendation for the making of such tests is given in National Electrical Manufacturers Association publication 107.[4] More specific test codes for particular types of apparatus are given in the NEMA publication for that type of apparatus.

V. Relation of Radio-Influence Voltages of Apparatus to Radio-Receiver Noise

Two methods have been used whereby the radio-influence voltage measured on power apparatus at the factory may be interpreted as having quantitative significance in terms of radio-receiver noise in an average case. In one we analyze the measurements made on new apparatus in the factory and the experience in the field with this apparatus to arrive at a reasonable limit. In the other we consider all the factors given in items 2 through 8 of section III of this paper to obtain values for the following:

Table III. Limits of Radio-Influence Voltage for High-Voltage Apparatus

Circuit Voltage Ratings (Range in Volts)	Insulation Classes (Kilovolts)	Limits of Radio-Influence Voltage (Microvolts)
1,201–15,000	1.2–2.5–5.0–7.5/8.7–15	1,000
15,001–37,000	23/25–34.5	2,500
37,001–73,000	46–69	5,000
73,001–145,000	92–115–138	10,000

1*a*. No limits are included for equipment at circuit voltage ratings above 145,000 volts because of present lack of data above this voltage class.

1*b*. These values do not apply to insulators, as an independent set of values has already been established for them by NEMA, or to motors and generators or thermionic devices, such as rectifiers and inverters.

1*c*. For switchgear assemblies there are not sufficient radio-influence voltage data on which to base a limit.

1*d*. For transformers the limits are based on insulation class only.

2. The radio-influence voltage limits given in this table apply to new apparatus tested in the factory in accordance with the report of the Joint Co-ordination Committee on Radio Reception of EEI, NEMA, and RMA, entitled "Methods of Measuring Radio Noise" (NEMA publication 107) 1940 and the apparatus test codes and test voltages for each type of apparatus, as approved by NEMA.

3. If apparatus of any given insulation class is used on a circuit of higher-circuit voltage rating, the radio-influence limit and the test voltage for the apparatus shall be that corresponding to its insulation class and not to the insulation class of the circuit on which it is used.

4. For special apparatus having more than one rating the radio-influence limit and test voltage corresponding to the highest rating shall apply.

1. The radio-influence voltage measured on the apparatus under standardized conditions (apparatus *RIV*).

2. The radio-influence voltage measured on a power line to which this apparatus is connected (line *RIV*).

3. The radio-influence voltage measured on or referred to a one-meter antenna at a standard position under this line (under line *RIV*).

4. The radio-noise voltage measured on an antenna in proximity to this power line and referred to a one-meter antenna (antenna *RNV*).

5. The signal strength of the radio program being received.

6. The signal to noise ratio in the receiving set.

7. The susceptibility of the receiver and antenna system.

1. Radio-Influence Voltage (*RIV*) of Apparatus

For several years we have been collecting measured radio-influence voltage values on a wide range of types of power

Table IV. Comparison of Apparatus to On-Line Radio-Influence Voltages

Test Voltage (Kilovolts)	Measured Radio-Influence Voltage on Apparatus (Microvolts)	Measured Radio-Influence Voltage (On-Line) At Apparatus Microvolts	Ratio	At End of Line (2 Miles) Microvolts	Ratio
20	14,000	9,400	1.5	2,600	5.4

apparatus and circuit voltages. These have been classified in three groups, that is, new apparatus, apparatus in service with no complaints, and apparatus removed because of radio noise and radio-noise complaints. These data have been plotted on a summary chart classified into three voltage ranges, and this chart is shown in Figure 2. In the 1,201–15,000-volt range some 950 individual tests are summarized; in the 15,001–37,000-volt range, 130 tests, and in the 37,001–73,000-volt range, 94 tests. Radio-influence voltages ranging from a few microvolts to 100,000 and above are shown.

From a study of such data it is possible to arrive at recommended levels for the limiting value of radio-influence voltage of apparatus. This has been done, and the recommended levels are shown on the curves and are also given in Table III. Such an approach to the question of limiting values is a direct one. It depends upon accumulating sufficient field experience with apparatus to obtain statistical data on which a conclusion may be reached. As time goes on and more data are accumulated, these can be used to confirm the original conclusions or to suggest desirable modifications of them.

Another approach to the problem begins with the values on apparatus and proceeds through the several steps already outlined, such as radio-influence voltage on the power line, under the line, and on the antenna, and signal strength and signal to noise ratio. This approach requires the establishment of representative values for each of these quantities and then combines them all together to arrive at an over-all ratio. The ratio can then be used to interpret radio-influence voltage of apparatus in terms of the quality of radio reception. This method will now be outlined.

2. RATIO OF APPARATUS *RIV* TO ON-LINE *RIV*

Having the *RIV* of a given piece of apparatus when measured with the standard factory setup, we must be able to form an estimate of the resulting *RIV* when this apparatus is connected to a representative power line. The factory tests are made with 600 ohms across the apparatus, which resistance is intended to simulate line-surge impedance. However, a wide range of impedance will always be met with on power circuits. It is, therefore, necessary to resort to practical measurements wherein factory tests are followed by connection of the apparatus to line, and corresponding line radio-influence voltage measurements are taken. One such test is summarized in Table IV.

This test made on a two-mile test line believed to be representative showed a ratio of 1.5 to 1 between apparatus *RIV* and on-line *RIV*. It also indicated an attenuation of 50 per cent radio-influence voltage per mile, which figure agrees closely with a series of tests made with sine waves on the same line.

3. RATIO OF ON-LINE *RIV* TO UNDER-LINE *RIV*

Since in measurements in the field it is not possible to make measurements of the on-line *RIV* except in special cases, the common procedure has been to make measurements under the line. Such measurements of under-line *RIV* can be correlated with the on-line *RIV* by selection of a suitable place under the line which can be used as a field-strength reference point. The field-strength reference position which has been chosen as most significant and practical is one immediately under the line and midway between the poles. The *RIV* measured on an antenna of one-meter effective height placed at this reference point is designated in this paper as the under-line *RIV*. Since this measurement is referred to a one-meter antenna, it gives the same numerical value as the field intensity of the noise phenomenon at the reference point which has been used by some investigators. Corresponding measurements made on the line and under the line under specified conditions will provide a ratio which can be used later to evaluate under-line noise field strengths. To provide such a ratio, a series of measurements was made on the experimental line previously mentioned using sine-wave line-voltage excitation at 1,000,000 cycles. This line averaged 28 feet in height and was terminated to ground through a surge impedance of 600 ohms, and measurements were made at several points along the line as indicated in Table V.

Table V. Comparison of On-Line to Under-Line Radio-Influence Voltages

Test Point Along the Line in Feet	On-Line *RIV* (Microvolts)	Under-Line *RIV* (Microvolts)	Ratio On Line / Under Line
0	75,000	950	79
900	58,000	1,400	42
1,400	53,000	1,040	51
5,400	28,000	420	67
8,500	24,000	800	30
10,000	16,500	270	61
			Average 55

The on-line to under-line ratios range from 30 to 79 with an average of 55. Two factors at least are known to have contributed to this variation. These were uncertainties of electrical contact on the conductor for the on-line measurements and variations in line height. The test results of Table V, however, represent an average field situation and may, therefore, be used to provide the quantitative ratio necessary to this particular step between apparatus *RIV* and radio-receiver noise.

4. RATIO OF UNDER-LINE *RIV* TO ANTENNA *RNV*

Next in order, it is necessary to establish a ratio between under-line and antenna voltages. If the antenna voltage is expressed in terms of a one-meter antenna, as previously described for the under-line measurement, such a significant ratio will result. A series of tests made in Canada by the radio division of the Department of Transport and directed by H. O. Merriman include a large number of field

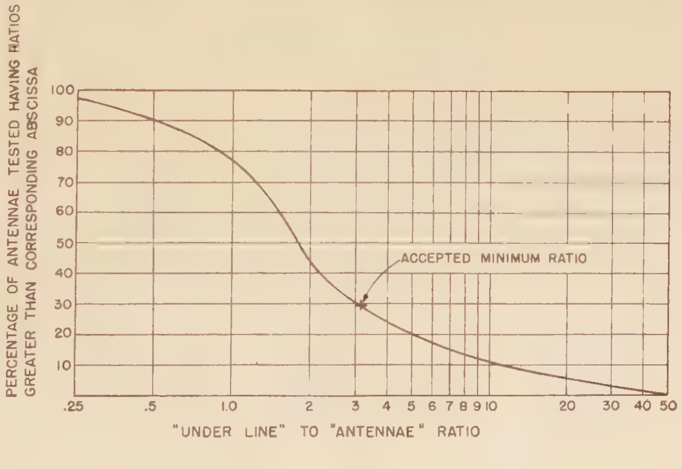


Figure 3. Under-line-to-antenna radio-influence-voltage ratios

Table VI. Test Voltages for Radio-Influence Voltage Measurements

I	II	III	Voltage Rating of Apparatus Normally Used				
			Distribution Cutouts		Lightning Arresters		
Circuit Voltage (Line-to Neutral/ Line-to-Line) (Volts)	Radio-Influence Test Voltage (Volts)	ASA Transformer Insulation Class (Kv)	IV Grounded (Kv)	V Un-grounded (Kv)	VI Grounded (Kv)	VII Un-grounded (Kv)	Switchgear VIII ..... (Kv)
1,440/2,500	1,580	2.5	2.5	2.5	3	3	2.5*
2,500/4,330	2,750	5.0	2.5*	5.0	3*	6	5.0
2,880/5,000	3,160	5.0	5.0*	5.0	6	6	5.0*
4,160/7,200	4,570	8.7	7.5	7.5	6	9	7.5*
5,000/8,660	5,500	8.7	7.5	15.0	6*	9	15.0
7,200/12,470	7,960	15.0	7.5*	15.0	9*	15	15.0
7,960/13,800	8,760	15.0	15.0	15.0	12*	15	15.0
8,650/15,000	9,500	15.0	15.0*	15.0	15*	20	15.0*
13,300/23,000	14,600	23.0			20*	25*	23.0*
19,990/34,500	21,900	34.5			30*	37*	34.5*
26,500/46,000	29,300	46.0			40*	50*	46.0*
39,500/69,000	43,900	69.0			60*	73*	69.0*
53,000/92,000	58,500	92.0			78*	97	115.0
66,500/115,000	73,100	115.0			97	121	115.0*
80,000/138,000	87,700	138.0			121*	145	138.0*
93,000/161,000	102,400	161.0			145*	169	161.0*
113,000/196,000	124,600	196.0			169*	205*	230.0
132,500/230,000	146,200	230.0			195*	242	230.0*
165,500/287,500	182,000	287.5			242*	302*	287.5*

* For the apparatus of columns IV through VIII an asterisk is placed on the line which gives the test voltage (column II) for each apparatus rating.

For transformers, test voltages shall be 110 per cent of the maximum rated line-to-neutral voltage but shall not exceed the maximum values (in column II) corresponding to the insulation class of the transformer of column III.

measurements bearing on this ratio. We are indebted to Mr. Merriman for permission to use these results. Measurements were obtained on 35 antennas ranging in effective heights from 0.2 to 9 meters and located from 20 to 4,500 feet from power lines. Line voltages ranged from 10 to 220 kv. It should be understood that the tests were conducted on outdoor antennas representing a selected group of installations in which radio interference from power lines was reported to be particularly troublesome. Under-line-to-antenna voltage ratios derived from these measurements ranged from 0.25 to 50. The higher this ratio the lower the field from the power line picked up by the receiver antenna and the better the antenna system for good reception. These values are plotted on Figure 3 against percentage of antennas tested as ordinate. This ordinate percentage gives the proportion of receiver stations tested having higher under-line-to-antenna ratios than indicated by the corresponding abscissas. Since this investigation was made mainly on "interference-affected" situations, the curve shows only 30 per cent of the antennas had a ratio higher than 3.2. The investigators estimated that, when all receivers were considered, approximately 90 per cent of them would have ratios higher than 3.2. In accordance with these tests, Canadian Practice[10] sets a value of 3.2 for the minimum permissible under-line-to-antenna ratio. This means that ratios less than this for line-to-antenna couplings do not measure up to the performance that reasonably may be required for good reception.

Table VII. Radio-Influence Voltage Limits o Plain Pin-Type Insulators

60-Cycle Dry Flashover Rating (Kv Rms)	Limit of Radio-Influence Voltage (Microvolts)
Up to and inc. 50	2,500
51 to 79	5,500
80 to 109	8,000
110 to 124	12,000
125 to 149	16,000
150 to 200	25,000

5. Signal Strength of Radio Program

The required signal strength of the radio program for good reception has been estimated by various engineers to range from 10,000 microvolts per meter in large cities to 100 microvolts for sparsely settled rural areas. The Federal Communications Commission recognizes the area for good reception of certain types of stations as that covered by a signal field strength of 500 microvolts and for some other classes field strengths as low as 100 microvolts. The Canadian regulations accept a level of signal field strength of 500 microvolts per meter as a lower limit above which good reception is practically and economically achievable.

It seems, therefore, reasonable to use the figure of 500 microvolts per meter as reference value for this study.

6. Signal-to-Noise Ratio

Several series of listener tests utilizing a range of types of radio noise at various signal strengths and noise levels have indicated that a signal-to-noise ratio of 32 to 1 is an average ratio for use as a criterion of good reception. This ratio is accepted in the Canadian regulations.[11]

A ratio of 40 decibels has been suggested by International Electrotechnical Commission as a suitable signal-to-noise value indicating, however, at the same time, that values higher than 30 decibels have not been achieved in practice. Also, the FCC protects clear channel stations with a signal-to-noise ratio of 20 decibels. The test ratio of 32 to 1 (30 decibels), therefore, seems appropriate.

7. Apparatus Limits Derived From These Ratios

Having discussed the several significant ratios and values, we may now use them to arrive at a reasonable value for the radio-influence voltage of apparatus. If the signal strength of 500 microvolts per meter at the radio receiver is divided by the signal-to-noise ratio of 32 to 1, an antenna radio-noise voltage of 15.6 microvolts is obtained. Using this value with the under-line-to-antenna ratio of 3.2 gives a value of 15.6 times 3.2 equal to 50 microvolts per meter, which is, therefore, an upper limit of noise field intensity at the reference position under the line. This value can now be used with the other ratios to obtain an apparatus limit. For instance, on the 28-foot line previously referred to, with an apparatus-to-on-line ratio of 1.5 and an on-line-to-under-line ratio of 55, the apparatus limit for radio-influence voltage will be 50 times 1.5 times 55 equal to 4,100 microvolts; also for a 35-foot line correcting the 55 ratio proportional to line height now becomes 70 to 1 and gives an apparatus limit for radio-influence voltage of 50 times 1.5 times 70 equal to 5,300 microvolts.

These figures have direct significance when compared with the apparatus limits based on direct experience with factory-measured values as described in section V—1 of this paper and given in Table III.

Table VIII. Radio-Influence Voltage Limits for "Radio-Freed" Pin-Type Insulators

60-Cycle Dry Flashover Rating (Kv Rms)	Limit of Radio-Influence Voltage (Microvolts)
Up to and inc. 50	50
51 to 79	50
80 to 109	100
110 to 124	100
125 to 149	200
150 to 200	200

The greater part of the field data and experimental investigation represented in this analysis are derived from lines of about 66-kv construction. The limit of 5,000 microvolts for this circuit voltage of Table III is between the level for 28-foot- and 35-foot-high lines. It is apparent, therefore, that the limits reached through both approaches are consistent. On lower-voltage lines the apparatus limits of 2,500 and 1,000 microvolts appear to have the necessary margin of safety consistent with their frequent location closer to radio receivers. Also, the higher-voltage range at 10,000 microvolts should be acceptable, because distances to receivers are greater, and because up to this time interference experience has not been sufficient to indicate any practical necessity of a lower limit.

The problem of practical limits for factory tests of radio-influence voltage on apparatus has now been considered from two standpoints, first that of direct experience with factory measurements on apparatus, and second that of experimental determination of significant coupling ratios between apparatus and receiver set. The limits arrived at from these two standpoints are in good agreement.

The values given in Table III have been adopted as limits by many NEMA sections for high-voltage apparatus.

VI. Radio-Influence Test Voltages for Apparatus

Radio-influence voltages on apparatus generally increase with increasing applied voltage. Consequently suitable influence-voltage limits will of necessity be tied in with applied test voltages which will bear definite relation to the apparatus circuit voltage. When this is done, all apparatus used on the same circuit will have a common standard test voltage. Because different kinds of apparatus have different rating bases, it has been found necessary to work out a common basis for as wide a range of apparatus types as possible.

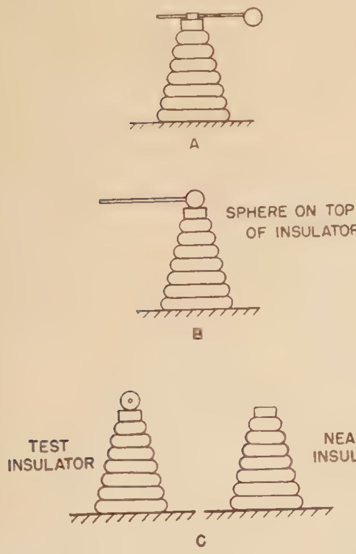


Figure 4. Radio-influence voltage tests on pedestal insulators

Table IX. Radio-Influence Voltage Measurements on Insulators (Effect of Field Configuration)

Radio-Influence Voltage in Microvolts

1 Test Voltage Applied (Kv—Rms)	2 Diagram A 10-Inch Sphere	3 Diagram A 5-Inch Sphere	4 Diagram B 10-Inch Sphere	5 Diagram C 10-Inch Sphere (2d Insulator Nearby)	6 Diagram C 10-Inch Sphere (2d Insulator Grounded)	7 Diagram C 10-Inch Sphere, 2d Insulator Moved to 72 Inches
80	7	55	185	7	120	17
100	29	78	225	22	205	35
120	52	98	225	55	270	70
140	92	135	290	110	360	120
160	144	155	1,100	230	840	360

The figures of column 3 show the increase in voltage obtained with the substitution of the smaller five-inch sphere for the ten-inch. The increase is particularly pronounced for the lower voltages. In the fourth column the ten-inch sphere was located immediately on top of the insulator, resulting in a marked increase in voltage due to discharge in the crevices between hardware and porcelain. The fifth column shows that just locating an insulator nearby gives a marked increase in voltage, presumably through changes in field configuration. Grounding the second insulator further increases the voltage as shown in column 6, and moving it further away gives the expected decrease of column 7. These results are typical and show how important are the details of circuit arrangements for such tests.

The circuit voltage determines the voltage stress to ground that is applied to the connected apparatus, and hence it appears logical that the circuit voltage should be used as the basis for establishing the test voltage. Since some apparatus is designed for use on several different circuit voltages, the standard test voltage for such apparatus should be based on the maximum circuit voltage for which such apparatus is rated.

It is recognized that in some cases the test voltage is less than the rated voltage of the apparatus. However, it will be found that in such cases the rating of the apparatus is not based upon the sustained voltage but upon abnormal conditions.

Table VI lists the various three-phase line-to-line circuit voltages with the corresponding line-to-neutral voltages. It gives a standard test voltage that is based on 110 per cent of the line-to-neutral circuit voltage and shows how standard apparatus normally would be applied and tested. For transformers the test voltage is 110 per cent of the maximum rated line-to-neutral voltage, which is the highest rated tap voltage for wye-connected transformers and the highest rated tap voltage divided by the square root of 3 for delta-connected transformers, but shall not exceed the highest values in column IV for the insulation class of the winding of column III. These values have been standardized by NEMA and appear to the extent required in the appropriate high-voltage-apparatus publications to which reference should be made for specific types.

VII. Insulators

Experience in the fields of apparatus and line insulators has indicated these to be occasional sources of radio interference. Probably the most common case is that wherein a radio-influence voltage results from the breakdown of a small air gap between tie wire and insulator or sparkover in a crevice of a defective insulator. Particular defective insulator units must be located and removed. Generally plain pin-type units will produce not more than the levels of influence voltage shown in Table VII.

In many localities well-served by broadcasting stations, plain pin-type insulators have been used satisfactorily. However, the values of Table VII are substantially above the apparatus limits of Table III. Accordingly, insulator users and manufacturers have directed attention to the reduction of radio-influence voltages for insulators. In this marked success has been achieved. One user in a locality

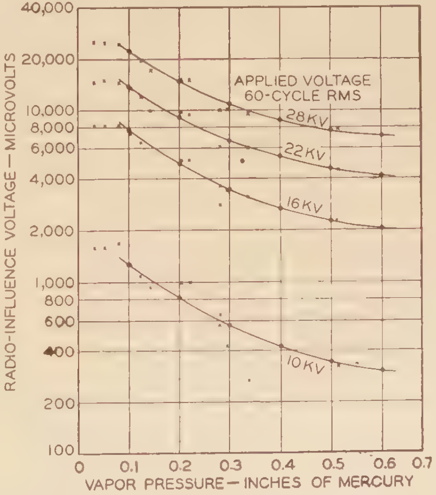


Figure 5. Variation of radio-influence voltage with humidity on high-voltage insulators

Table X. Radio-Influence-Voltage Tests on Insulators
(Effect of Paralleling Units)

Test Voltage Applied (Kv—Rms)	Insulator Units (Microvolts), Individual Tests								Paralleled Units (Microvolts)			Calculated Root-Sum-Square		
	1*	2	3	4	5	6	7	8	6-7	3, 5, 6, 7	All 8	6-7	3, 5, 6, 7	All 8
10	5,100	4,900	4,300	3,410	4,200	4,300	4,300	3,700	5,900	7,600	11,500	6,100	8,000	12,200
16	14,500	17,500	17,500	13,500	17,500	17,500	15,500	13,500	19,500	27,000	35,000	23,500	34,000	45,000
22	25,000	32,000	32,000	24,000	32,000	31,000	27,000	23,000	30,000	46,000	64,000	42,500	62,500	82,000
28	44,000	48,000	49,000	39,000	46,000	42,000	39,000	39,000	49,000	73,000	97,000	57,000	88,000	122,000

* The eight insulators used were numbered 1 to 8.

not well-served by broadcasting stations has worked out a method of applying an asphalt emulsion[12,13] to each insulator which gives very low levels. Insulator manufacturers have given considerable attention to the problem, and as a result "radio-freed" pin-type insulators are now available.

In an effort to set up reference standards significant to the levels achievable through this special design, the insulator group of NEMA has agreed upon the values of Table VIII.

Since we have two types of insulators, the plain and radio-freed, and two corresponding sets of limits for radio-noise-influence characteristics, it seems logical to assume that these limits are based upon the values it is possible to achieve under the manufacturing conditions pertaining to each type of insulator.

VIII. Radio-Influence-Voltage Variables in Apparatus Testing

As high-voltage apparatus testing for radio influence has progressed, experience has focused attention on several factors which affect apparatus levels in very pronounced measure. Among these are voltage field configurations, paralleled apparatus units, time of voltage application, and humidity.

1. Voltage Field Configurations

Considerable variation in testing may result when dielectric field distributions around test connections between supply voltage apparatus and test-piece and measurement equipment are altered. Very largely the threshold voltage at which considerable noise begins and the amount of influence voltage as dependent upon the number and extent of field-voltage gradients concentrated at particular points of small radius of curvature on the test piece or test circuit. Such field concentrations will be altered in degree with every change of circuit, and it is, therefore, important that attention be given to them in making a test setup. To illustrate this effect a brief description of a test wherein it is involved in a very conspicuous manner is given.

Table XI. Comparison of Measured with Calculated Values on Paralleled Units

Test Voltage (Kilovolts)	Ratio: $\frac{\text{Root-Sum-Square Value}}{\text{Measured Value}}$ Insulators 6 and 7	Insulators 3, 5, 6, 7	All 8
10	1.03	1.13	1.06
16	1.31	1.26	1.29
22	1.42	1.36	1.28
28	1.10	1.21	1.26
		Average ratio	1.23

A pedestal insulator was being tested by arrangements shown in Figure 4. Voltages were applied to arrangements A, B, and C, and radio-influence voltages were measured. The results are given in Table IX.

2. Paralleled Units of the Same Apparatus

Radio-influence voltages generally increase when additional units of a particular type are added to the test circuit. The amount of this increase has not been confirmed by sufficient data to provide for good understanding on this point. According to local experience the custom has ranged from using the single-unit value to multiplying it by the number of units in parallel. Results of a practical test on this point are summarized in Table X.

It is obvious from these results that additional units increase the measured radio influence definitely, but that this increase is not in proportion to the number of units added. Some observation of high-voltage radio-interference phenomena by oscillograph has shown the voltage waves to be quite variable in duration, amplitude, and time of occurrence. When additional units are added, presumably crests do not correspond, or the new waves are added in time between the old. For practical purposes the method of estimating the results for added units by the root-sum-square method has been found satisfactory. This method is applied by adding the squares of individual unit results and taking the square root of this sum. Applied to the results of Table X, values as given in Table XI are obtained.

It is apparent that, calculated in this manner, values somewhat higher than the combined measured values are obtained. This test shows the calculated values to range some 23 per cent higher than the measured. A value thus obtained is considered to be a good conservative estimate.

3. Time Factor in Voltage Application

Radio-influence voltage varies with time of voltage application, particularly within the first few minutes. This is illustrated by a test summarized in Table XII made on a pin-type insulator at 22 kv and at 0.34 inch of mercury atmospheric vapor pressure.

Table XII. Radio-Influence-Voltage Variation With Time of Applied Voltage

Time (Minutes)	Radio-Influence Voltage (Microvolts)
0	7,700
1	6,000
2	5,600
3	5,000
4	4,500
5	4,400
10	4,200
15	3,900
20	3,800

In this case, as is usually found with insulators, the radio-influence voltage decreased rapidly at first and then slowly, so that in some 20 minutes a constant value was obtained. Some other types of apparatus show an increasing influence voltage with time of application. Experience, therefore, indicates that, to obtain values which will check from day to day or from one test set to another, a common time of voltage application is required. The one-minute value gives ample time for a short-time test and is practical in that it does not require excessive waiting. However, the longer time values are possibly more in agreement with the continuous values consistent with service performance of apparatus. A five-minute test generally has been viewed as a good compromise and is now contained in a number of apparatus test codes.

4. HUMIDITY

Measurement experience has shown that in many cases atmospheric humidity has a substantial effect on radio-influence voltages of apparatus. A good example is that of porcelain insulators, with which this variation may be as much as 4 to 1 over the normal range of atmospheric humidity conditions. A specimen-test result showing influence-voltage variations at several voltage levels on a standard pin-type insulator is shown in Figure 5.

This test was made in a closed chamber in which the atmosphere could be artificially reduced by suitable dryers, so that a range from 0.03 inch of mercury vapor pressure to 0.6 inch was obtained. The humidity was measured with a dew point potentiometer, and data of radio-influence voltage variations were obtained at 10 kv, 16 kv, 22 kv, and 28 kv. The marked increase in voltage as humidity decreased is consistent at all voltage levels tested. The normal test voltage for this insulator is 22 kv, and the limiting value of radio-influence voltage, 12,000 microvolts from Table VII.

Adding moisture until precipitation takes place has a varied effect. When water droplets fill in small crevices between conductor and insulation, in which local breakdown occurs, the influence voltage will decrease, because the water shifts the voltage across the crevice to other parts of the dielectric in series. But the converse of a higher influence voltage may result, if droplets form projections of small radius on an otherwise smooth surface. In this case the voltage gradient at the droplet increases, and additional local disruption gives rise to increased radio-influence voltage. Humidity then is another factor contributing to influence voltage level which must be reckoned with in testing and considered in cases of field interference.

IX. Conclusions

1. Use of the present available radio-noise meters when utilized only with the internal calibrator gives results within ±50 per cent in measurements on sine waves of radio frequency.

2. Use of a sine-wave signal generator to calibrate the noise-meter results in marked improvement and permits obtaining checks on sine waves of radio frequency to within eight per cent.

3. Even when calibrated on a signal generator, the results with different radio-noise meters, when used to measure on a given square wave, may differ by 37 per cent. Differing types of apparatus noise also result in similar meter differences.

4. As an improved instrument-performance specification, it is recommended that one or more calibration waves of varied and representative shapes be adopted.

5. Limits for the radio-influence voltage of apparatus derived on the basis of apparatus measurement and field experience are given in Table III. These are the same as the values adopted by many sections of NEMA for high-voltage apparatus.

6. To bridge the gap between radio-influence voltages of apparatus measured in the factory and noise measured on a radio-receiver set, a series of values is found valuable. These ratios are—

(*a*). Apparatus-to-on-line ratio, 1.5 to 1.

(*b*). On-line-to-under-line ratio, 70 to 1.

(*c*). Under-line-to-receiver-antenna ratio, 3.2 to 1.

(*d*). Signal-to-noise ratio, 32 to 1.

7. Apparatus limits for radio-influence voltage given in Table III and derived on the basis of apparatus measurement and field experience agree with limits as found through the use of field measurements and ratios as in conclusion 6.

8. Based on these ratios and assuming an antenna signal strength of 500 microvolts per meter and a signal-to-noise ratio of 32 to 1, a relative radio-influence voltage level for apparatus limits is derived. This level agrees with those of Table III based on factory tests and field experience.

9. To provide for the testing of all apparatus used on the same circuit, a standard set of test voltages for many types of high-voltage apparatus has been standardized as given in Table VI.

10. Factors which must be controlled in the measurement of radio-influence voltage in apparatus are voltage field configurations, number of test parts in parallel, time of voltage application, and humidity. To provide good measurement checks, these factors should be included in individual apparatus test codes.

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Open-Delta Transformer Banks—Analysis of Circuit

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FELLOW AIEE

WHEN a three-phase transformer bank is operated in open delta, an unbalanced secondary voltage results, and the transformers themselves may be overloaded. The unbalanced voltage will cause a circulating current to flow which may cause overheating of the equipment in the secondary circuit to an extent depending upon the amount of the voltage unbalance and the load being carried. Wolf and Mattison[1] have described a method of overcoming the voltage unbalance, and this paper is written to mention briefly the effect of unbalanced voltages on the operation of induction motors and to present an analysis of the circuit, together with some correlated material.

Open-delta operation of transformers will set up negative-sequence voltages in the secondary circuit which may cause increased heating of induction motors. Whether or not this increased heating is serious depends upon the load being carried by the motors and their temperature-rise characteristics.

The unbalance of the secondary voltages can be reduced to a negligible value by the addition of capacitance to the circuit, and a method is given of estimating the required amount and its distribution.

Existing methods of calculating regulation of open-delta transformer banks are not suitable for the analysis of this problem, and a new method is presented together with a method of calculating the positive and negative components of the current necessary for the application of the new method.

Effect of Unbalanced Voltages on Motor Operation

The unbalanced voltages resulting from open-delta operation are detrimental to the operation of induction motors in the secondary circuit and result in a reduction in torque and an increase in heating. The amount of voltage unbalance depends, not only upon the reactance of the transformer, but also upon the magnitude and type of load. The greatest unbalance will occur when all the load is static and the least when all the load is rotating. However, a given negative-sequence reactance applied to an induction motor produces a definite negative-sequence current of a magnitude depending only upon the characteristics of the motor.

There is a reduction in torque, because the positive-sequence voltage applied to the motor is reduced. For example, in Figure 4 of the Wolf–Mattison paper, the positive-sequence line-to-neutral secondary voltage for the closed-delta case is 335.2 volts, with an applied line-to-line equivalent voltage of 615 volts, and it is reduced to 319.8 volts for the open-delta case, with line-to-line equivalent voltage of 622. This represents a reduction in torque of nine per cent, and, in addition, the negative-sequence voltage introduces a small (usually negligible) torque in the reverse direction. Since the required mechanical output is unchanged, the current must increase to maintain the load, and increased heating results.

The unbalanced regulation of the open-delta bank introduces a negative-sequence voltage which, in turn, causes negative-sequence current to flow, and a further increase in heating results. The negative-sequence impedance of induction motors may be in the order of 10 to 25 per cent, so that a relatively small per-unit negative-sequence voltage causes a relatively larger current to circulate. The increase in losses is illustrated in Figure 1 for two types of induction motors. A ten per cent negative-sequence voltage may cause an increase in total loss, ranging from 33 to 93 per cent at full load, depending upon the characteristics of the motor.

The extra loss is revealed as increased heating in the motor. If the excess loss were evenly distributed among the three phases, the overheating would be proportional roughly to the excess losses. However, the current unbalance is such that the overcurrent flows through one phase of wye-connected motors or through two phases of delta-connected motors, resulting in local overheating and relatively greater excess temperatures than indicated by the over-all excess losses.

Whether or not this overheating is serious will depend upon the loading of the motor and the closeness of the design. If the motor is underloaded or has a liberal margin in the temperature-rise performance, the increased heating probably will not be harmful. If, however, the motors are fully loaded and already operating at a temperature close to the maximum temperature for which a reasonable length of life may be expected, the overheating caused by the excess losses may be disastrous.

Regulation of Open-Delta Transformer Banks

The usual approximate formulas[2] given for the calculation of the regulation of open-delta transformer banks are derived upon the assumption of balanced voltages and currents. Since this condition does not exist in the secondary circuit of an open-delta bank, the results are in error to a greater or less degree, and the formulas cannot be used for an analysis of this nature.

The regulation of an open-delta transformer bank may be developed as follows. See Figure 2, Figure 3a, and Figure 8.

The voltage and current relationships are

$$\begin{aligned} \boldsymbol{E}_{AB}&=\boldsymbol{E}_{ab}+\boldsymbol{I}_{ab}Z_t & \boldsymbol{I}_{ab}&=\boldsymbol{I}_B \\ \boldsymbol{E}_{BC}&=\boldsymbol{E}_{bc}+\boldsymbol{I}_{bc}Z_t & \boldsymbol{I}_{bc}&=0 \\ \boldsymbol{E}_{CA}&=\boldsymbol{E}_{ca}+\boldsymbol{I}_{ca}Z_t & \boldsymbol{I}_{ca}&=-\boldsymbol{I}_C \end{aligned} \tag{1}$$

and from these

$$\begin{aligned} \boldsymbol{E}_{AB}&=\boldsymbol{E}_{ab}+\boldsymbol{I}_BZ_t=\boldsymbol{E}_{ab}+\boldsymbol{e}_{ab} \\ \boldsymbol{E}_{BC}&=\boldsymbol{E}_{bc}+(\boldsymbol{I}_C-\boldsymbol{I}_B)Z_t=\boldsymbol{E}_{bc}+\boldsymbol{e}_{bc} \\ \boldsymbol{E}_{CA}&=\boldsymbol{E}_{ca}-\boldsymbol{I}_CZ_t=\boldsymbol{E}_{ca}+\boldsymbol{e}_{ca} \end{aligned} \tag{2}$$

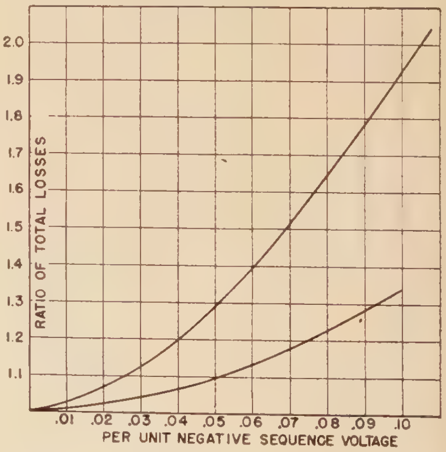

Figure 1. Variation in total loss of two types of induction motors caused by negative-sequence voltage

Motors maintain full mechanical output, and positive-sequence voltage decreases

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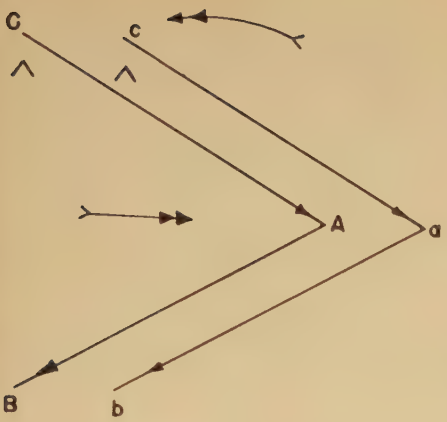


Figure 2 (left). Representation of open-delta transformer bank with direction of power flow indicated

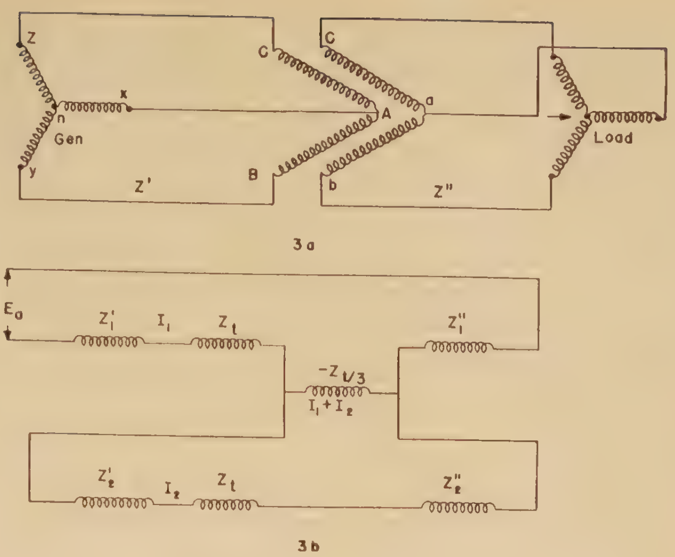


Figure 3 (right). Representation of complete circuit with open-delta transformer bank and the corresponding equivalent circuit

In the foregoing it is presumed that the phase angle of $\boldsymbol{I}_B$ in reference to $\boldsymbol{E}_B$, and so forth, is known.

If we write

$$\begin{aligned} \boldsymbol{e}_{ab} &= \boldsymbol{I}_B Z_t &&= (\boldsymbol{I}_{B1}+\boldsymbol{I}_{B2})Z_t \\ \boldsymbol{e}_{bc} &= (\boldsymbol{I}_C-\boldsymbol{I}_B)Z_t &&= (\boldsymbol{I}_{C1}-\boldsymbol{I}_{B1}+\boldsymbol{I}_{C2}-\boldsymbol{I}_{B2})Z_t \\ \boldsymbol{e}_{ca} &= -\boldsymbol{I}_C Z_t &&= -(\boldsymbol{I}_{C1}+\boldsymbol{I}_{C2})Z_t \end{aligned} \tag{3}$$

it can be shown easily from formula 3 and Figure 8 that

$$\boldsymbol{e}_{ab1} = \frac{(-1+a^2)(2\boldsymbol{I}_{A1}-\boldsymbol{I}_{A2})}{3} Z_t$$

and

$$\boldsymbol{e}_{a1} = \frac{2\boldsymbol{I}_{A1}-\boldsymbol{I}_{A2}}{3} Z_t \tag{4}$$

$$\boldsymbol{e}_{ab2} = \frac{(-1+a)(-\boldsymbol{I}_{A1}+2\boldsymbol{I}_{A2})}{3} Z_t$$

and

$$\boldsymbol{e}_{a2} = \frac{-\boldsymbol{I}_{A1}+2\boldsymbol{I}_{A2}}{3} Z_t$$

and also

$$\begin{aligned} \boldsymbol{e}_a &= \frac{\boldsymbol{I}_{A1}+\boldsymbol{I}_{A2}}{3} Z_t &&= \frac{\boldsymbol{I}_A}{3} Z_t \\ \boldsymbol{e}_b &= \frac{(2a^2-a)\boldsymbol{I}_{A1}+(2a-a^2)\boldsymbol{I}_{A2}}{3} Z_t \\ &= \frac{2\boldsymbol{I}_B-\boldsymbol{I}_C}{3} Z_t \\ \boldsymbol{e}_c &= \frac{(2a-a^2)\boldsymbol{I}_{A1}+(2a^2-a)\boldsymbol{I}_{A2}}{3} Z_t \\ &= \frac{2\boldsymbol{I}_C-\boldsymbol{I}_B}{3} Z_t \end{aligned} \tag{4a}$$

From formula 4 and Figure 3a we can write the expressions for the total positive and negative voltages in the circuit. They are

$$\begin{aligned} \boldsymbol{E}_a &= \boldsymbol{I}_1\left(Z_1+\frac{2Z_t}{3}\right)-\boldsymbol{I}_{a2}\frac{Z_t}{3} \\ 0 &= -\boldsymbol{I}_1\frac{Z_t}{3}+\boldsymbol{I}_2\left(Z_2+\frac{2Z_t}{3}\right) \end{aligned} \tag{5}$$

These expressions, formula 5, yield the equivalent circuit shown in Figure 3b. It is interesting to note that this is identical with the equivalent circuit for open-delta transformer banks originally developed by Edith Clarke in 1934 using an entirely different approach.

The sequence currents may be calculated as follows:

$$\boldsymbol{I}_{a1} = \boldsymbol{E}_a \frac{Z_2+\frac{2}{3}Z_t}{D} \tag{6}$$

$$\boldsymbol{I}_{a2} = \boldsymbol{I}_{a1} \frac{Z_t/3}{Z_2+\frac{2}{3}Z_t} \tag{7}$$

in which

$$D = \left(Z_1+\frac{2}{3}Z_t\right)\left(Z_2+\frac{2}{3}Z_t\right)-\frac{1}{9}Z_t^2 \tag{8}$$

Z_1 = positive-sequence impedance external to the transformer

Z_2 = negative-sequence impedance external to the transformer

Again we write from formulas 2 and 3

$$\begin{aligned} \boldsymbol{E}_{AB} &= \boldsymbol{E}_{ab}+\boldsymbol{I}_{B1}Z_t+\boldsymbol{I}_{B2}Z_t \\ \boldsymbol{E}_{BC} &= \boldsymbol{E}_{bc}+(\boldsymbol{I}_{C1}-\boldsymbol{I}_{B1})Z_t+(\boldsymbol{I}_{C2}-\boldsymbol{I}_{B2})Z_t \\ \boldsymbol{E}_{CA} &= \boldsymbol{E}_{ca}-\boldsymbol{I}_{C1}Z_t-\boldsymbol{I}_{C2}Z_t \end{aligned} \tag{9}$$

In the foregoing the voltage drop in the open-delta bank between the primary and secondary windings is made up of the drop resulting from two symmetrical currents. Also, we may look upon the primary voltages as those existing before the transformers are loaded, and therefore they are a set of symmetrical voltages.

This permits a simplification in the expression to the following:

$$\begin{aligned} \boldsymbol{E}_{ab} &= E_{AB}-(I_{A1}Z_t\epsilon^{j(\phi+\theta_1+30)}+ I_{A2}Z_t\epsilon^{j(\phi+\theta_2-90)}) \\ \boldsymbol{E}_{bc} &= E_{BC}-(\sqrt{3}I_{A1}Z_t\epsilon^{j(\phi+\theta_1)}- \sqrt{3}I_{A2}Z_t\epsilon^{j(\phi+\theta_2)}) \\ \boldsymbol{E}_{ca} &= \boldsymbol{E}_{CA}-(I_{A1}Z_t\epsilon^{j(\phi+\theta_1-30)}+I_{A2}Z_t\epsilon^{j(\phi+\theta_2+90)}) \end{aligned} \tag{10}$$

In this equation the angle ϕ is the angle of the transformer impedance, θ_1 and θ_2 are the phase angles of the positive- and negative-sequence currents, I_{A1} and I_{A2}, in reference to $\boldsymbol{E}_A$. The angles θ and θ_2 are negative for lagging current and positive for leading current.

The foregoing equation 10 permits the determination of the voltage drop for each phase, the second term on right side of each expression. When this voltage drop is expressed on a per cent or per-unit basis, any of the conventional expressions[3] may be used to calculate the regulation, depending upon the degree of accuracy required.

In each of the foregoing equations 10 the reference vector for each secondary voltage is its respective primary voltage. On further expansion and with the usual approximations introduced, the positive-

Table I

	Measured	Derived	Measured	Derived
Kw	982	982	975	975 (982)
I_A	1,500	1,500 $\epsilon^{-j35.29}$	1,200	1,200 $\epsilon^{-j36.50}$
I_B	1,130	1,130 $\epsilon^{j167.88}$	1,150	1,150 $\epsilon^{-j167.96}$
I_C	1,110	1,110 $\epsilon^{j96.17}$	1,150	1,150 $\epsilon^{j84.96}$
E_{ab}	578	578 $\epsilon^{-j152.34}$	602	602 $\epsilon^{-j150.39}$
E_{bc}	538	538 $\epsilon^{j85.99}$	595	595 $\epsilon^{j88.90}$
E_{ca}	545	545 $\epsilon^{-j29.50}$	592	592 $\epsilon^{-j30.16}$
E_A		328.8		345.1
E_B		324.9 $\epsilon^{-j124.28}$		346.8 $\epsilon^{-j120.94}$
E_C		305.3 $\epsilon^{j118.49}$		341.0 $\epsilon^{j119.28}$
I_a			700	700 $\epsilon^{j59.5}$
I_{bc}			350	350 $\epsilon^{-j1.29}$
I_c			900	
I_{bt}				840 $\epsilon^{-j148.40}$
I_{ct}				829 $\epsilon^{j136.65}$

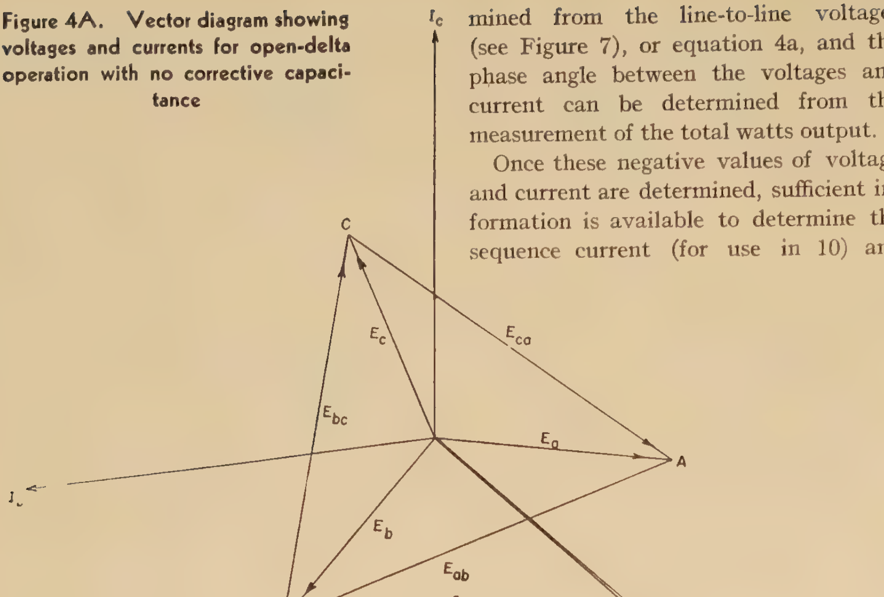


Figure 4A. Vector diagram showing voltages and currents for open-delta operation with no corrective capacitance

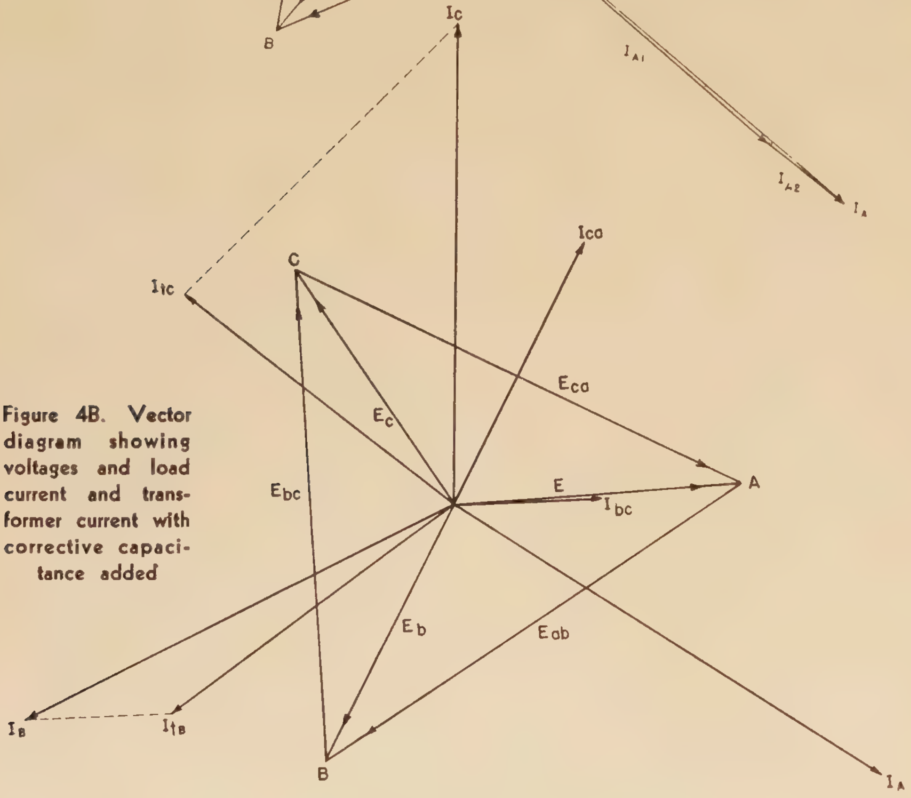


Figure 4B. Vector diagram showing voltages and load current and transformer current with corrective capacitance added

sequence parts will yield the conventional form[2] given for the calculation of the regulation of open-delta transformer banks.

Assume three voltage values E_{ab}, E_{bc}, and E_{ca}, together with three current values I_A, I_B, and I_C, have been measured. Then we can write (see Figure 7)

$$\begin{aligned} \boldsymbol{E}_{ab} &= E_{ab} & \boldsymbol{I}_A &= I_A \\ \boldsymbol{E}_{bc} &= E_{bc}\epsilon^{-j(180-\boldsymbol{ab})} & \boldsymbol{I}_B &= I_B\epsilon^{-j(180-\boldsymbol{AB})} \\ \boldsymbol{E}_{ca} &= E_{ca}\epsilon^{j(180-\boldsymbol{ca})} & \boldsymbol{I}_C &= I_C\epsilon^{j(180-\boldsymbol{CA})} \end{aligned} \tag{11}$$

in which the angles are determined as indicated in Figure 7, but it should be noted fully that the voltage angle $\boldsymbol{ab}$ need not be the same necessarily as the current angle $\boldsymbol{AB}$. The line-to-neutral voltages, $\boldsymbol{E}_a$, $\boldsymbol{E}_b$, and $\boldsymbol{E}_c$, may be determined from the line-to-line voltages (see Figure 7), or equation 4a, and the phase angle between the voltages and current can be determined from the measurement of the total watts output.

Once these negative values of voltage and current are determined, sufficient information is available to determine the sequence current (for use in 10) and then to determine the per-unit impedance voltage (12 and 12a) in magnitude and phase angle, to be used in the calculation of regulation. On the other hand, if the constants of the circuit are known, the sequence currents may be evaluated by use of equations 6 and 7.

New Formula for Precise Calculation of Transformer Regulation

Some of the conventional approximate formulas[3] for regulation are subject to appreciable error when the per-unit drop is above the usual values. Accordingly, the following new method of calculating regulation is offered, which permits calculation to any desired precision.

Express one of the foregoing expressions as:

$$E_s = E_p - (I_{b1}Z_t\epsilon^{j(\phi+\theta_1+30)} + I_{b2}Z_t\epsilon^{j(\phi+\theta_2-30)})$$

$$= E_p - E_{ps}\epsilon^{j\theta} \tag{12}$$

anb then, if we let

$$p = \frac{E_{ps}}{E_s} \tag{12a}$$

and then

$$\frac{E_p}{E_s} = 1 + p\epsilon^{j\theta}$$

there results

$$\frac{E_p}{E_s} = 1 + p\cos\theta + jp\sin\theta \tag{13}$$

Per-unit regulation,

$$r = \frac{E_p - E_s}{E_s} = \frac{E_p}{E_s} = 1$$

so that from equation 13

$$r = a - \frac{a^2}{2} + \frac{a^3}{2} - \frac{5}{8}a^4 + \frac{7}{8}a^5 - \frac{21}{16}a^6 + \frac{33}{16}a^7 - + \ldots \tag{14}$$

$$a = p\cos\theta + \frac{p^2}{2}$$

The expression $p\epsilon^{j\theta} = p\ (\sin\theta + j\cos\theta)$ represents the per-unit impedance drop in magnitude and position. For this particular problem the proper values to use are obtained from equation 10.

By this method the regulation may be calculated to any precision justified by the supporting data, and this method has been incorporated by the writer in the latest revision of American Standard for Transformers, *C*-57.1, and was included in the report of the subcommittee responsible for the revision of section 4 of *C*-57.

Calculation of Regulation

At this point the regulation for the open-delta condition of case 2 of Figure 4 of the Wolf–Mattison paper will be

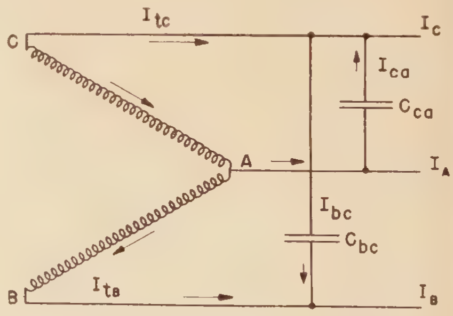


Figure 5. Secondary side of open-delta transformer bank showing phases to which corrective capacitance is added

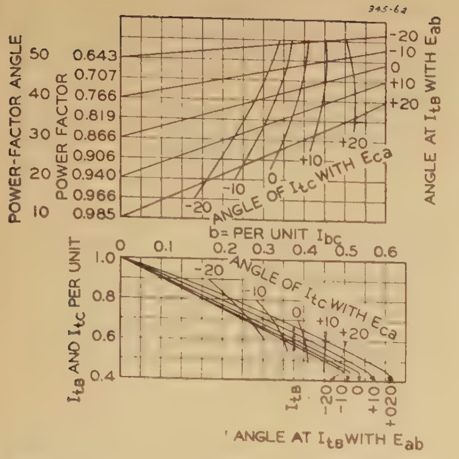


Figure 6A. Chart showing relationship between the amount of corrective capacitance added, the resultant current in the transformer, and the phase angle between the transformer current and the transformer voltages

For $n=\frac{I_{ca}}{I_{bc}}=1^2$

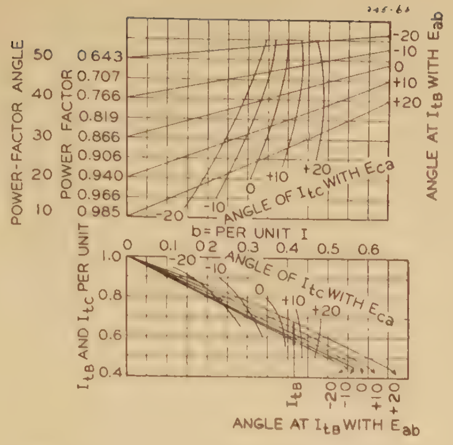


Figure 6B. Chart showing relationship between the amount of corrective capacitance added, the resultant current in the transformer, and the phase angle between the transformer current and the transformer voltages

For $n=\frac{I_{ca}}{I_{bc}}=2$

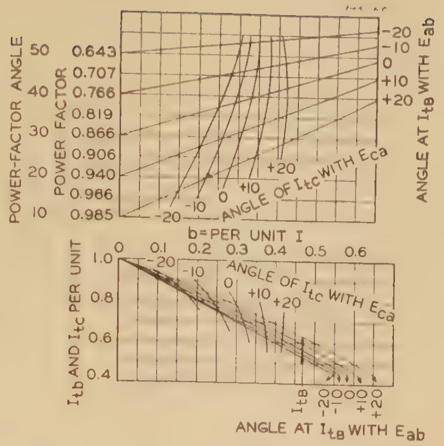


Figure 6C. Chart showing relationship between the amount of corrective capacitance added, the resultant current in the transformer, and the phase angle between the transformer current and the transformer voltages

For $n=\frac{I_{ca}}{I_{bc}}=2^1/_3$

calculated. The per-unit impedance of the transformer is 0.0592 at 522 amperes, and the impedance angle ϕ is 82.52 degrees. (All numerical values of angles are in degrees.)

Then

$$I_1=(1{,}230-j8.7)\epsilon^{-j35.29}=1{,}230\epsilon^{-j35.70}$$
$$I_2=(270+j8.7)\epsilon^{-j35.29}=270.1\epsilon^{-j33.44}$$
$$Z_t=0.0592\epsilon^{j82.52}$$

In the preceding equation for I_1 and I_2, the reference vector is E_a, whereas in equation 10 the reference vector is E_A. The angle between E_A and E_a may be found from the following:

$$\tan\beta=\frac{I_AZ_t\sin(\phi+\theta)}{3+I_AZ_t\cos(\phi+\theta)}$$

which gives

$$\beta=0.78$$

and then $\theta_1=36.48$, and $\theta_2=34.22$, so that

$$\phi+\theta_1+30=76.04$$

and

$$\phi+\theta_2-90=41.70$$

$$\phi+\theta_1=46.04$$

and

$$\phi+\theta_2=48.3$$

$$\phi+\theta_1-30=16.04$$

and

$$\phi+\theta_2+90=138.30$$

$$\frac{1{,}230}{522}\times0.0592=0.1395$$

$$\frac{270.1}{522}\times0.0592=0.03064$$

Then for phase AB

$$p\epsilon^{j\theta}=0.1395\,\epsilon^{j76.04}+0.03064\epsilon^{-j41.70}$$
$$=0.0566+j0.115$$
$$a=0.0648$$

Per-unit regulation $=0.0628$ for phase AB

And in a similar manner for the other phases there results the data shown in Table A.

This is a reasonably good check. The per-unit values are all on the basis of the rated output voltage—575 volts.

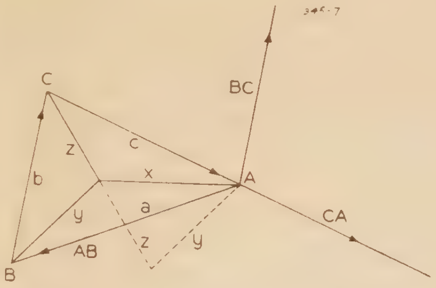


Figure 7. Vector diagram illustrating simplified method of finding line-to-neutral voltage and the positive- and negative-sequence components from a set of unbalanced line-to-line voltages, when no zero-sequence voltage is present

Analysis of Data, Figure 4 of the Wolf–Mattison Paper

In Table I is shown an analysis of the data in Figure 4 of the Wolf–Mattison paper, giving the measured and certain derived values. The phase angle between E_a and I_A is predicated upon the requirement that the calculated output of the two transformers must equal the measured value. This information is shown in Figures 4a and 4b.

The positive- and negative-sequence components for the open-delta operation, Figure 4a, are without capacitance.

$$E_{a1}=319.4-j16.38$$
$$I_{A1}=(1{,}230-j8.7)\epsilon^{-j35.29}$$
$$E_{a2}=9.4+j16.83$$
$$I_{A2}=(270+j8.7)\epsilon^{-j35.29}$$

and with corrective capacitance added

$$E_{a1}=344.3-j3.31 \qquad I_{A1}=1{,}166.5\epsilon^{-j36.50}$$

$$E_{a2}=0.85+j3.31 \qquad I_{A2}=33.5\epsilon^{-j36.50}$$

$$\cos ab=\frac{a^2+b^2-c^2}{2ab} \qquad \cos xy=\frac{x^2+y^2-z^2}{2xy} \qquad \cos cx=\frac{c^2+x^2-z}{2cx}$$

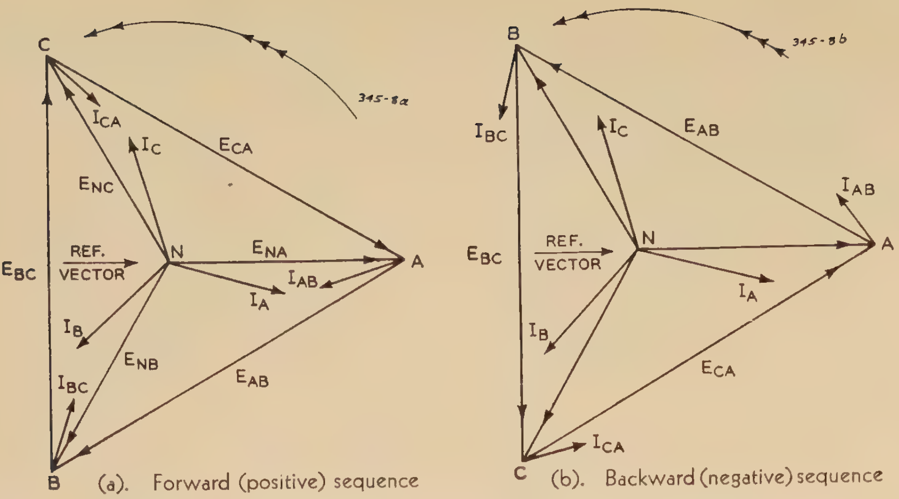
Other angles in similar manner. All angles acute.

$$X^2=\frac{1}{9}(2a^2-b^2+2c^2),\ a^2=2x^2+2y^2-z^2,\ \text{and so forth}$$

$$E_{AB}=E_{AB} \qquad E_a=E_a \qquad E_{AB}=E_{AB}\epsilon^{-j(180-xa)}$$
$$E_{BC}=E_{BC}\epsilon^{-j(180-ab)} \qquad E_b=E_b\epsilon^{-j(180-xy)} \qquad E_{BC}=E_{BC}\epsilon^{j(xa+ab)}$$
$$E_{CA}=E_{CA}\epsilon^{j(180-ca)} \qquad E_c=E_c\epsilon^{j(180-zx)} \qquad E_{CA}=E_{CA}{}^{-}\epsilon^{j(ca-xa)}$$

$$E_{AB1}=\frac{1}{2}E_{AB}+j\frac{1}{2\sqrt{3}}(E_{BC}-E_{CA})jE_{AB2}=\frac{1}{2}E_{AB}-j\frac{1}{2\sqrt{3}}(E_{BC}-E_{CA})$$

$$E_{a1}=\frac{1}{2}E_{AB}+j\frac{1}{2\sqrt{3}}(E_B-E_C)jE_{a2}=\frac{1}{2}E_a-j\frac{1}{2\sqrt{3}}(E_B-E_C)$$



(a). Forward (positive) sequence

(b). Backward (negative) sequence

Figure 8. Chart showing certain sequence relationships used in the text

Positive Sequence

$$E_a = E_a\epsilon^{j0} = E_a \qquad I_a = \qquad = I_a\epsilon^{j\theta}$$

$$E_b = E_a\epsilon^{-j120} = a^2E_a \qquad I_b = I_a\epsilon^{j(\theta-120)} = a^2I_a\epsilon^{j\theta}$$

$$E_c = E_a\epsilon^{-j240} = aE_a \qquad I_c = I_a\epsilon^{j(\theta-240)} = aI_a\epsilon^{j\theta}$$

$$E_{ab} = (-1+a^2)E_a = \sqrt{3}E_a\epsilon^{-j150} \qquad I_{ab} = \frac{I_a}{-1+a} = \frac{1}{\sqrt{3}}I_a\epsilon^{j(\theta-150)}$$

$$E_{bc} = (-a^2+a)E_a = \sqrt{3}E_a\epsilon^{j90} \qquad I_{bc} = \frac{I_a}{-a+a^2} = \frac{1}{\sqrt{3}}I_a\epsilon^{j(\theta+90)}$$

$$E_{ca} = (-a+1)E_a = \sqrt{3}E_a\epsilon^{-j30} \qquad I_{ca} = \frac{I_a}{-a^2+1} = \frac{1}{\sqrt{3}}I_a\epsilon^{j(\theta-30)}$$

$$E_a = \frac{1}{\sqrt{3}}E_{ab}\epsilon^{-j150} \qquad I_a = \sqrt{3}I_{ab}\epsilon^{j\theta}$$

$$E_b = \frac{1}{\sqrt{3}}a^2E_{ab}\epsilon^{j150} \qquad I_b = \sqrt{3}a^2I_{ab}\epsilon^{j\theta}$$

$$E_c = \frac{1}{\sqrt{3}}aE_{ab}\epsilon^{j150} \qquad I_c = \sqrt{3}aI_{ab}\epsilon^{j\theta}$$

Use subscript 1, for example, E_{a1}

Negative Sequence

$$E_a = E_a\epsilon^{j0} = E_a \qquad I_a = \qquad = I_a\epsilon^{j\theta}$$

$$E_b = E_a\epsilon^{-j240} = aE_a \qquad I_b = I_a\epsilon^{j(\theta-240)} = aI_a\epsilon^{j\theta}$$

$$E_c = E_a\epsilon^{-j480} = a^2E_a \qquad I_c = I_a\epsilon^{j(\theta-480)} = a^2I_a\epsilon^{j\theta}$$

$$E_{ab} = (-1+a)E_a = \sqrt{3}E_a\epsilon^{j150} \qquad I_{ab} = \frac{I_a}{-1+a^2} = \frac{1}{\sqrt{3}}\,I_a\epsilon^{j(\theta+150)}$$

$$E_{bc} = (-a+a^2)E_a = \sqrt{3}E_a\epsilon^{-j90} \qquad I_{bc} = \frac{I_a}{-a^2+a} = \frac{1}{\sqrt{3}}\,I_a\epsilon^{j(\theta-90)}$$

$$E_{ca} = (-a^2+1)E_a = \sqrt{3}E_a\epsilon^{j30} \qquad I_{ca} = \frac{I_a}{-a+1} = \frac{1}{\sqrt{3}}\,I_a\epsilon^{j(\theta+30)}$$

$$E_a = \frac{1}{\sqrt{3}}\,E_{ab}\epsilon^{j150} \qquad I_a = \sqrt{3}I_{ab}\epsilon^{j\theta}$$

$$E_b = \frac{1}{\sqrt{3}}\,aE_{ab}\epsilon^{-j150} \qquad I_b = \sqrt{3}a\epsilon^{j\theta}$$

$$E_c = \frac{1}{\sqrt{3}}\,a^2E_{ab}\epsilon^{-j150} \qquad I_c = \sqrt{3}a^2E^{j\theta}$$

Use subscript 2, for example, E_{a2}

In these expressions E_a is the reference vector.

From these data it is seen that open-delta operation in this particular case introduces 6 per cent negative-sequence voltage, which in turn introduces a 22 per cent negative-sequence current.

In Figure 4b the voltage and load currents are seen to be closely balanced, and this has been accomplished by the addition of capacitance current in certain proportions (see Figure 5) as described by Wolf and Mattison.

The current I_B in the leading phase *AB* has been advanced slightly in phase (Figure 4b), and thus its phase angle is reduced in reference to the voltage *AB*, and at the same time it is reduced in magnitude. The current I_C in the leading phase has been advanced greatly in phase, and this also reduces its (reversed) phase angle in reference to the voltage *CA* and, at the same time, its magnitude. After this modification has been made, current I_{bt} leads its voltage E_{ab} by 1.99 degrees and the current I_{ct}, reversed, lags its voltage E_{ca} by 13.19 degrees. Since the power factor is now practically unity, the difference in regulation is so slight that the negative-sequence voltage introduced has a negligible effect.

The relative values of capacitance current that need be added will depend upon the power factor and magnitude of the load. The required amount and its distribution cannot be determined readily by a study of the unbalanced condition. Rather the line currents I_B and I_C *for balanced three-phase conditions* must be modified so that the respective transformer currents, I_{tB} and I_{tC}, are not only approximately equal in magnitude, but also have approximately the same phase relationship to their respective voltages, E_{ab} and E_{ca}. By this procedure the unequal regulation is reduced greatly, and thereby the cause of the unbalanced current is removed and, consequently, its harmful effects.

Table A

Phase	Calculated	Test
AB	0.0628	0.0765
BC	0.1407	0.146
CA	0.1120	0.1373

Strictly speaking, the proper capacitance for one power factor is not correct at other power factors. In Figure 6 are given curves (see also Figure 5), by which the amount of corrective capacitance may be estimated, and it should be borne in mind that precision is not necessary.

In these curves b is the per-unit capacitive current, based on actual load current, added across phase *bc*, and nb represents the capacitive current added across phase *ca*, with n having values of $1\frac{2}{3}$, 2, and $2\frac{1}{3}$.

These charts are used as follows: Assume the power factor of the load is 0.77, assume b to be 0.35 as the first trial, and n as $1\frac{2}{3}$. Then the intersection of the line for power factor = 0.77 and $b=0.35$ indicates an angle of about +1.5 degrees for I_{tC} and of about −24 degrees for I_{tB} on the upper chart. Now locate these angles respectively on the lower chart for the assumed values of $b=0.35$; I_{tC} is indicated as about 0.71, and I_{tB} is indicated as about 0.65. This amount of correction probably would not be satisfactory, and a new trial should be made with a different b or different n, or with both different.

For a further example, assume a load having a power factor of 0.81 or $\theta=36$ degrees. Then we can get the results from the curves as shown in Table II.

With the possible exception of the first entry, any of the foregoing will give sufficient reduction in the unbalance to assure satisfactory operation of the induction motors on the secondary circuits. Incidentally, the fourth entry

Table II

n	b	θ_{tB}	θ_{tC}	I_{tB}	I_{tC}
$1\frac{2}{3}$	0.3	+4	−21	0.730	0.725
$1\frac{2}{3}$	0.35	+6	−13	0.690	0.710
$1\frac{2}{3}$	0.4	$+8\frac{1}{2}$	− 3	0.655	0.755
2	0.3	+4	$-13\frac{1}{2}$	0.730	0.700
2	0.35	+6	− 3	0.690	0.730
2	0.4	$+8\frac{1}{2}$	$+6\frac{1}{2}$	0.655	0.750
$2\frac{1}{3}$	0.3	+4	$-5\frac{1}{2}$	0.730	0.665
$2\frac{1}{3}$	0.35	+6	+ 4	0.690	0.640
$2\frac{1}{3}$	0.4	$+8\frac{1}{2}$	$+15\frac{1}{2}$	0.655	0.660

Skin Effect in Bimetallic Conductors

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Synopsis: Mathematical expressions for a-c impedance and current distribution are derived for bimetallic conductors with circular symmetry. The results are illustrated by application to a copper-covered steel conductor in the frequency range up to 160 kilocycles. Measurements of the a-c resistance in the same frequency range provide a check on the theoretical results. The studies made on the copper-covered steel conductor indicate that its resistance is substantially that of its tubular copper portion alone and, for all but the lowest frequencies, may be taken from the simpler formulas and curves already available for tubes.

BIMETALLIC conductors consisting of a copper covering welded to a steel core are widely used for power and communication lines, because the great tensile strength of these conductors permits long pole spans. The resistance of such bimetallic conductors at power frequencies is known.[1] A knowledge of the resistance at higher frequencies, in the range up to 150 kilocycles, has become important for designing and for determining the performance of carrier-current communication systems operated over bimetallic conductors.

A previous study[2] of this subject was published in 1915 by J. M. Miller, who gave an approximate method of calculating a-c resistance and compared calculated with experimental results for several specimens of conductors in the frequency range up to three kilocycles. However, his method of calculation is based upon the assumption that the current density in the outer layer of copper is uniform, which, although it is an excellent approximation at low frequencies, departs considerably from the actual condition at high frequencies.

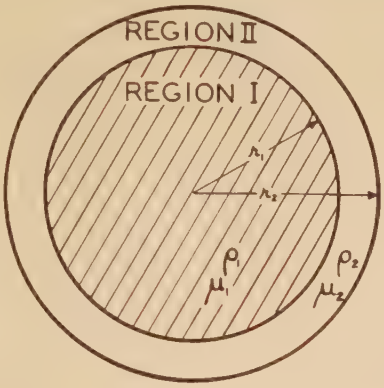

Figure 1. Section of bimetallic conductor

It is the purpose of this investigation to develop a more exact theory of skin effect for bimetallic conductors with circular symmetry, to illustrate the theory by using it to calculate for a particular copper-covered steel conductor the ratio of a-c resistance to d-c resistance and the current distribution over a range of frequencies, and to check the theory by an experimental determination of the resistance ratio for the same conductor over the selected range.

Theoretical Expression for A-C Impedance

In what follows, a general relation is developed for the a-c impedance of a conductor which has the section shown in Figure 1. The interior region I has resistivity ρ_1 and permeability μ_1; the exterior region II, ρ_2 and μ_2. The developed relation applies to all other bimetallic conductors of circular section, including aluminum-covered steel, as well as to copper-covered steel.

In developing the theory it is assumed that:

1. Each region is homogeneous; that is, within each region the permeability and resistivity do not vary from point to point.
2. At any point the permeability and resistivity do not vary with time.
3. The bounding surface between regions and the outer surface are circular cylinders.
4. The conductor carries a sinusoidal current.
5. The conductor is very long and straight and the return conductor or conductors are either concentric tubes or at such remote distances that they produce no appreciable field within the conductor considered.

The derivation of the expression for the ratio of a-c impedance to d-c resistance is outlined in Appendix A. The expression itself, in convenient form for calculation, is:

$$\frac{Z_{ac}}{R_{dc}}=\frac{br_2}{2j}P\,\text{conj}\left[\frac{A-\frac{F}{H}C-IGA+\frac{IE}{H}C}{B-\frac{F}{H}D-IGB+\frac{IE}{H}D}\right] \quad (1)$$

in which P is the per-unit conductivity (per cent conductivity divided by 100), and each of the nine quantities A to I is a Bessel function expressed as a complex number, except for G and I, which are quantities simply obtained from Bessel functions. Other forms of Bessel functions may be employed, the present forms being selected for convenience in

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The authors acknowledge the contributions of two previous Buhl research fellows: H. E. Webb, Jr., who made a preliminary experimental study of the subject in his master of science thesis, and A. G. Williamson who made numerous measurements and aided in the development of the experimental technique. The conductors used in the tests were supplied by the Copperweld Steel Company.

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is the case covered by Figure 4 of the Wolf–Mattison paper, and the check is very good. In selecting the particular values of corrective capacitance the power factor and range of load are the factors to be considered.

Appendix

1. If there is no zero-sequence voltage in the system, then the proper neutral point of the system is the intersection of the medians of the closed line-to-line voltage triangle. This can be proved easily by geometric relationships and is true, because the line-to-neutral voltages must form a closed triangle of themselves and therefore fix the line-to-line triangle in such a way that any line-to-neutral voltage if extended, will bisect the opposite side. These relationships are set forth in Figure 7, which gives a direct method of calculating the line-to-neutral voltages from the line-to-line voltages when there is no zero-sequence voltage. Also, a more direct method of determining the positive- and negative-sequence components is given.

2. In Figure 8 are shown certain sequence relationships to which reference was made in the test.

References

1. MOBILE CAPACITOR UNITS FOR EMERGENCY LOADING OF TRANSFORMERS IN OPEN DELTA, H. B. Wolf, G. G. Mattison. AIEE TRANSACTIONS, volume 62, 1943, February section, pages 83–6.

2. TRANSFORMER ENGINEERING (book). John Wiley and Sons, New York, N. Y. Page 181.

3. American Standard for Transformers, C-57.1.

Table I. Equivalent Forms of Bessel Functions

$$
\begin{array}{lllll}
I_0(j^{1/2}z) = \text{ber } z + j \text{ bei } z & = & = J_0(j^{3/2}z) & = \text{conj } J_0(j^{1/2}z) & = M_0\epsilon^{j\theta_0} \\
I_1(j^{1/2}z) = \dfrac{1}{j}(\text{ber}_1 z + j \text{ bei}_1 z) & = \dfrac{1}{j^{1/2}}(\text{ber}' z + j \text{ bei}' z) & = \dfrac{1}{j^{1/2}}\dfrac{d}{dz}J_0(j^{3/2}z) & = j \text{ conj } J_1(j^{1/2}z) & = \dfrac{1}{j}M_1\epsilon^{j\theta_1} \\
K_0(j^{1/2}z) = \text{ker } z + j \text{ kei } z & = & = & = \dfrac{\pi}{2j} \text{ conj } H_0^1(j^{1/2}z) & = N_0\epsilon^{j\varphi_0} \\
K_1(j^{1/2}z) = j(\text{ker}_1 + j \text{ kei}_1 z) & = j^{3/2}(\text{ker}' z + j \text{ kei}' z) & = & = -\dfrac{\pi}{2} \text{ conj } H_1^1(j^{1/2}z) & = jN_1\epsilon^{j\varphi_1}
\end{array}
$$

See references 3, 6, and 7.

using the Jahnke–Emde tables.[3] The quantities are defined as follows:

$$
\left.
\begin{array}{ll}
\omega = 2\pi f & A = J_0(j^{1/2}br_2) \\
\quad = \sqrt{-1} & B = j^{1/2}J_1(j^{1/2}br_2) \\
 & C = H_0^{(1)}(j^{1/2}br_2) \\
a = \sqrt{\dfrac{\omega\mu_1}{\rho_1}} & D = j^{1/2}H_1^{(1)}(j^{1/2}br_2) \\
 & E = J_0(j^{1/2}br_1) \\
b = \sqrt{\dfrac{\omega\mu_2}{\rho_2}} & H = H_1^{(1)}(j^{1/2}br_1) \\
F = J_1(j^{1/2}br_1) & \\
G = \dfrac{H_0^{(1)}(j^{1/2}br_1)}{H_1^{(1)}(j^{1/2}br_1)} & \\
I = \sqrt{\dfrac{\mu_2\rho_2}{\mu_1\rho_1}} \times \dfrac{J_1(j^{1/2}ar_1)}{J_0(j^{1/2}ar_1)} & \\
P = 1 - \left[\dfrac{r_1}{r_2}\right]^2\left[1 - \dfrac{\rho_2}{\rho_1}\right] &
\end{array}
\right\} \quad (2)
$$

In the above expressions the meter-kilogram-second system of units[1] is employed.

The real component of Z_{ac}/R_{dc} is the ratio of a-c to d-c resistance; the imaginary component is the ratio of the reactance arising from the flux within the conductor to the d-c resistance.

The first terms of the numerator and denominator, A and B, are the largest terms for the cases that have been calculated and can be taken from the tables directly in Cartesian form. The other terms within the brackets, which are products and quotients, are obtained conveniently in polar form. Thus the arrangement in equation 1 is such that accuracy is not lost in the most important terms by writing them as the products of various factors, each of which, taken from a table, has some uncertainty in the last decimal place. Also if a slide rule is to be used, multiplication and division are required only in the less significant terms.

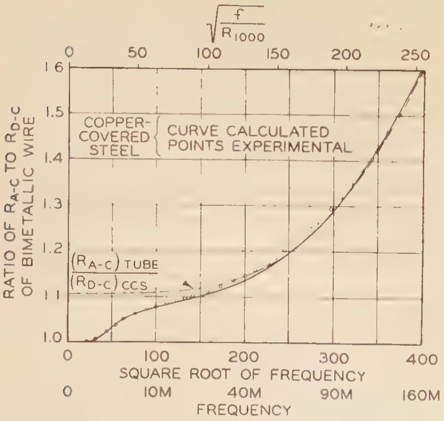

Figure 2. Ratio of a-c to d-c resistance, number 10 American wire gauge copper-covered steel, 40 per cent conductivity wire at 29 degrees centigrade

Another feature of the arrangement of the terms in equation 1 is that, by dropping terms and making appropriate changes in P, the expression gives the ratio Z_{ac}/R_{dc} for solid and hollow homogeneous conductors. Thus, if only the first terms A and B within the brackets are used, and if P is set equal to unity, the expression applies to a solid homogeneous wire of outer radius r_2, resistivity ρ_2, and permeability μ_2: that is, with the characteristics of the outer material. Likewise, if only the first two terms of the numerator and the first two terms of the denominator are used with P corresponding to an infinite value of ρ_1, the expression applies to a tubular conductor of inner and outer radii r_1 and r_2 made of the outer material.

Application of the Theory

To illustrate the use of equation 1 and to obtain calculated values of the ratio of R_{ac} to R_{dc} for comparison with experimental ones, the equation is applied to a particular specimen of number 10 American wire gauge copper-covered steel 40 per cent conductivity wire supplied by the Copperweld Steel Company.

The quantities r_1, r_2, ρ_1, ρ_2, and μ_1 were carefully determined for the conductor used. The value of r_2, the outer radius, was found from the average of a large number of micrometer measurements; ρ_1, the resistivity of the steel core material, was found from Kelvin-bridge measurements after the copper was removed chemically from the composite wire; ρ_2, the resistivity of the copper sheath, was taken from tables. The value of r_1 was calculated from the measured d-c resistance of the composite conductor and the previously determined values of r_2, ρ_1 and ρ_2. The permeability μ_1 of the core was determined from inductance measurements. For this purpose short lengths of the steel core were drilled and arranged for stringing, as cylindrical beads, on a small insulated wire with coaxial return. The inductance of the wire with and without the steel beads was measured by means of an inductance bridge, and from these measurements and the dimensions the permeability was calculated.

The following values were found:

$r_1 = 0.1024 \times 10^{-2}$ meter
$r_2 = 0.1303 \times 10^{-2}$ meter

$\rho_1 = 27.91 \times 10^{-8}$ ohm-meter
$\rho_2 = 1.830 \times 10^{-8}$ ohm-meter

$\mu_1 = 78.8 \times 4\pi \times 10^{-7}$
$\mu_2 = 1.0 \times 4\pi \times 10^{-7}$

The calculated ratio of a-c to d-c resistance as a function of frequency for a conductor with the above constants is shown by the solid curve in Figure 2. Values of frequency were chosen that would minimize the necessity for interpolation in the tables of Bessel functions.

When the scale of abscissas is expressed in terms of $\sqrt{f/R_{dc}}$ the curve of Figure 2 will apply to a conductor of any size, provided it is made of the same materials as the one for which the figure was calculated and has the same ratio of r_1 to r_2. These conditions also imply the same per cent conductivity. Such a scale is given

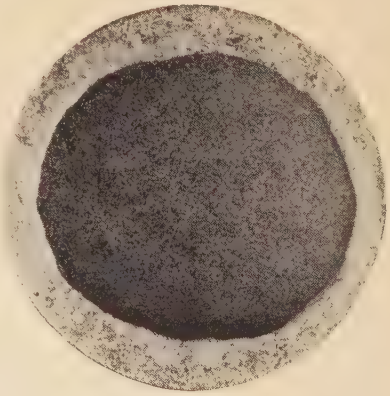

Figure 3. Photomicrograph of section of copper-covered steel wire

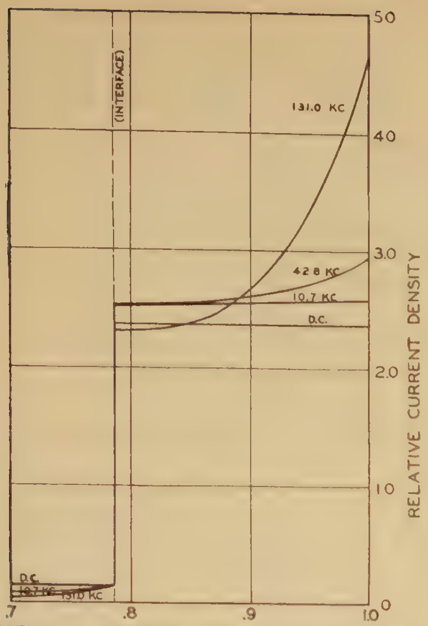

Figure 4. Variation of current density with distance from center of wire

at the top of the figure, R_{1000} being the d-c resistance in ohms per thousand feet.

Experimental Verification

The points in Figure 2 were obtained experimentally by the method described in Appendix B.

The agreement between calculation and experiment is everywhere at least within $1^1/_2$ per cent, at most frequencies is closer, and thus is well within most commercial tolerances. The experimental results are believed to be accurate to one quarter of one per cent; the calculations to one tenth of one per cent. The small discrepancies between calculated and test values are thought to have their origin in the departure of the actual conductor from the assumptions made in the analysis. In the particular specimen studied the section of the steel core is not exactly circular as may be seen from the photomicrograph of Figure 3. Also, micrometer measurements of the steel core, from which the surrounding copper had been removed chemically, showed a section of roughly elliptic form of major axes 0.075 and 0.085 inch. Another important departure from the assumptions lies in the nonhomogeneity of the copper. In the manufacture of copper-covered steel conductors, molten copper is poured around a steel core, and the resulting billet is rolled into rod and then drawn into wire. There is evidence that some iron migrates into the copper at the junction of the two metals, and the presence of even small amounts of iron causes an increase in the resistivity of the copper, this increase, however, probably being restricted to the immediate neighborhood of the interface.

Current Distribution

The current distribution over the section of the conductor, that is, the current density as a function of radial distance, is given by expressions that are derived in Appendix A. The result is shown in Figure 4, in which there is given a curve proportional to the magnitude of current density for frequencies of zero, 10.7, 42.8, and 131 kilocycles. Current densities are shown in only the outer portion of the conductor, the current density being small and unimportant in the interior regions at most frequencies. The curves are drawn to show the relative current densities on the assumption that the total current is the same at each of the four frequencies. Thus, as the frequency is increased, the current density in the interior falls with respect to that at the outer surface, and, to maintain the same total current, the density at the outside surface must increase, and this change results in a larger voltage drop per-unit length. In fact, the impedance drop per-unit length, exclusive of that part originating in flux linkages outside of the conductor, is proportional to the current density at the outside surface. Since the current is assumed to be the same for all frequencies Z_{ac}/R_{dc} is also proportional to this density, shown by the ordinate at extreme right of Figure 4.

The scale of ordinates for Figure 4 is chosen so that if the total current were distributed uniformly over both the steel and copper of the conductor, unit current density would result. In terms of this unit, at zero frequency the current density in the steel is 15.5 per cent, in the copper 236 per cent, the average, weighted with respect to area, being 100 per cent. On the other hand, at the highest frequency of the figure, 131 kilocycles, the current density at the center is substantially zero, on the steel side of the interface it is 15 per cent (nearly the same as before), on the copper side of the interface 231 per cent, and at the outside surface 464 per cent. If the current densities were averaged over the area of the conductor and account were taken of the variation of phase as well as magnitude, the result would again be 100 per cent.

The regularity of the changes with increase of frequency is not apparent from Figure 4; therefore, curves, showing the magnitude of the current density G_2 at the outside and G_1 at the copper side of interface, as functions of frequency are given in Figure 5, in which the current densities are expressed in terms of the average current density as used in Figure 4. Figure 5 also gives the phase of the current density at the interface and at the outer surface with respect to the total current in the conductor. For the interface the angles are lagging; for the surface, leading.

Since the steel carries but a small fraction of current, the most convenient criterion of the extent of skin effect is the ratio of the extreme magnitudes of current density in the copper: that is, of the ratio of interface density to surface density. This is given in Figure 6, together with another curve showing the angle by which the interface current density lags the surface density. Thus, as is shown in the figure, the change of phase from the inside to the outside of the copper is zero for direct current, 10 degrees for 10.3 kilocycles, and 82 degrees for 131 kilocycles. The corresponding change of phase, not shown in the figure, from the interface to the center of the steel core is zero for direct current, 182 degrees for 10.3 kilocycles, and 727 degrees for 131 kilocycles.

Figures 4, 5, and 6 are expressed in terms of frequency or its square root and apply to the specific conductor under consideration. However, they, like Figure 2, could be generalized to apply to any other conductor of different radius but of the same materials and with the same per cent conductivity.

The study of current distribution throws light upon the reasons for the small departures of the theoretical curve from

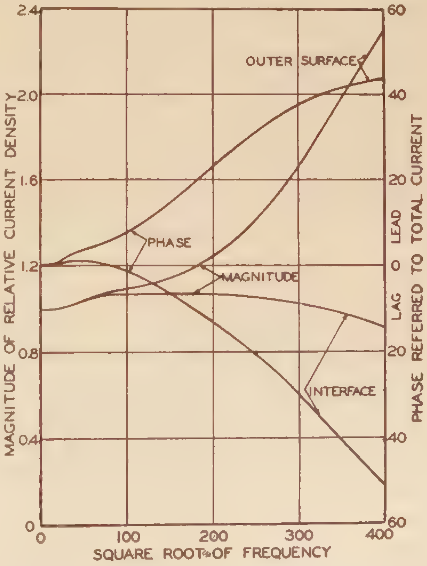

Figure 5. Magnitude and phase of current densities at interface and outer surface

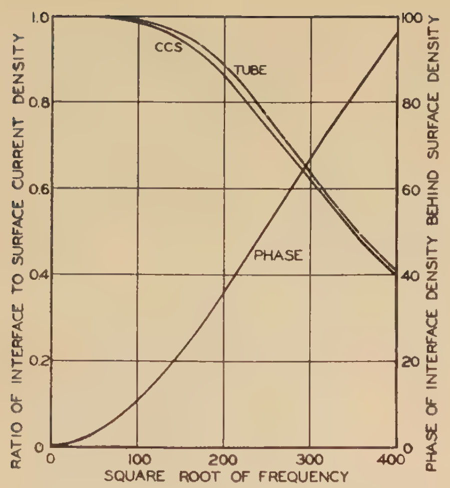

Figure 6. Ratio of interface to surface current density, and corresponding phase difference

the experimental points in Figure 2. The fact that these departures occur at intermediate frequencies rather than at the extremes indicates that they probably are caused by inaccuracies associated with the interface. At zero frequency there is no error, because the constants are made to agree with d-c measurements. At low frequencies, below ten kilocycles, the rise in the resistance ratio is caused by a decrease of current density in the steel core. The decrease is greatest at the center, and there is little change at the interface or in the copper; thus, conditions at the center determine the shape of the curve at the low frequencies. On the other hand, at the highest frequencies, 100 to 160 kilocycles, the current density in the steel is negligible, and that in the interface region is overshadowed by the higher current densities at the outside surface; thus, conditions at the outside surface are predominant in determining the curve at the highest frequencies. However, at intermediate frequencies there is appreciable current density on both sides of the interface, and it is the change of this density with frequency that determines the shape of the resistance ratio curve. Thus, at the intermediate frequencies the geometric irregularity and interfacial rise in the copper resistivity would have their greatest effect. The small discrepancies at the highest frequencies may be caused by the lack of agreement between the table value and the actual value of the resistivity of copper. At these frequencies inaccuracies in the constants ρ_1 and μ_1 of the iron have but little effect on the calculated values of R_{ac}/R_{dc}.

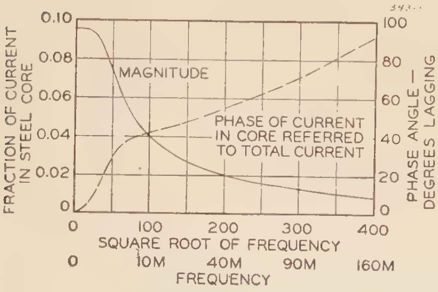

Figure 7. Magnitude and phase of the fraction of total current carried by steel core

Approximate Method of Determining A-C Resistance

The fact that but a small portion of the current flows in the steel core of the conductor in the special case studied, only 9.5 per cent even at zero frequency, suggests a simple approximate method of determining the a-c resistance of such a conductor, a method in which the current in the steel is neglected entirely and the resistance of the copper-covered steel conductor taken as the resistance of the tubular copper shell alone. This latter resistance is well known, is obtainable from relatively simpler formulas,[4] and is to be found conveniently in general curves.[4,5]

The accuracy of this method depends upon two conditions: that the current in the steel core contributes but little to the total current, and also that it has a negligible effect on the current distribution in the copper.

The fraction of the total current that is carried in the steel core is shown in Figure 7; also is shown the lagging phase angle of the total current in the core behind the phase angle of the current caused by the conductor as a whole. Starting at 10.5 per cent of the total current at zero frequency, the core current falls to 4 per cent at 10 kilocycles and to 1 per cent at 150 kilocycles. Meanwhile the phase angle of the core current behind the total current increases rapidly from zero to about 40 degrees in the first 10 kilocycles, and then more slowly to 90 degrees in the range from 10 to 150 kilocycles.

For the special conductor studied, the current in the core has a negligible effect upon the current distribution in the copper. Removing the core would cause a discernible change of current density only near the interface and this change is shown by the dashed curve in Figure 6. The difference in the phase change through the copper caused by removing the core is too small to show in Figure 6.

Finally, the total effect of treating the copper-covered steel conductor as a copper tube alone is shown in Figure 2, where the a-c resistance of the tube is referred to the d-c resistance of the composite conductor. Above 10 kilocycles there is less than four per cent difference between the a-c resistances of composite conductor and tube, above 20 kilocycles the approximate treatment is as good as the accurate one.

Thus, for many purposes it may be sufficiently accurate to treat the copper-covered steel conductor as a copper tube at the higher frequencies. The d-c resistance is known, and it should be possible to estimate a transition curve from this value to the curve for the tube with an error of only a few per cent.

Comparison of Several Conductors

Finally, it is of interest to compare the a-c resistances of the bimetallic conductor and the copper tube with that of a solid copper wire of the same outside diameter. This is done in Figure 8, which gives the ratios of the a-c resistances of the respective conductors to the d-c resistance of the copper-covered steel. Thus the ordinates are also proportional to the a-c resistances of the conductors for any common length. At zero frequency the resistance of the solid wire is 42 per cent, that of the tube 111 per cent of the resistance of the copper-covered steel. As the frequency increases, the resistance of the solid wire increases most rapidly, and at 85 kilocycles all conductors have approximately the same a-c resistance. At frequencies above 85 kilocycles the solid wire has the largest resistance as shown on the curve, but at still higher frequencies beyond the range of Figure 8 the curves would again coincide, when the current in all cases would be confined to a thin skin at the outermost boundary. From zero to 85 kilocycles the copper-covered steel has a lower resistance than the copper tube, but, between 85 and 160 kilocycles, the latter frequency being the highest calculated, the copper-

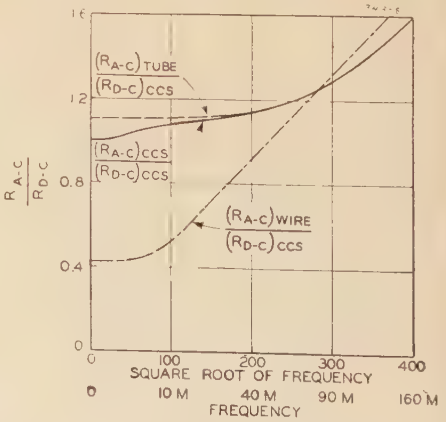

Figure 8. Comparison of a-c resistance of copper wire, copper tube, and copper-covered steel conductor of same outside diameter

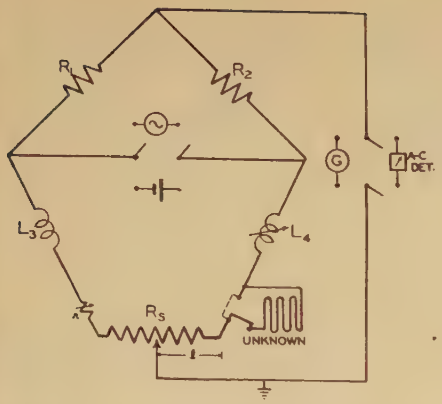

Figure 9. Circuit for measuring resistance ratio

covered steel has a slightly higher resistance than the tube alone.

Appendix A. Derivation of Equations

The current density in each of the regions must satisfy the differential equation

$$\frac{d^2G}{dr^2}+\frac{1}{r}\frac{dG}{dr}-j\frac{\omega\mu}{\rho}G=0 \qquad (3)$$

in which G is a complex number representing the magnitude and phase of the current density at any distance r from the axis. The derivation of the equation may be found in numerous treatments of skin effect, for example, the textbook by Woodruff.[1] The solution must also satisfy the boundary conditions:

1. At $r=0$, G remains finite.
2. At $r=r_1$, ρG is continuous.
3. At $r=r_1$, $(\rho/\mu)(dG/dr)$ is continuous.

The last two conditions come from the field equation

$$\text{Curl } \mathbf{E}=-\frac{\partial B}{\partial t} \qquad (4)$$

which for spatial circular cylindrical symmetry, axially flowing currents, and quantities varying sinusoidally in time reduces to

$$\frac{dE}{dr}=-j\omega B \qquad (5)$$

E being the electric field, and equal to ρG. Thus

$$\frac{d(\rho G)}{dr}=-j\omega B=-j\omega\mu H \qquad (6)$$

Since B is not infinite, ρG must be continuous. On either side of the interface, ρ is constant so

$$\frac{\rho}{\mu}\frac{dG}{dr}=-j\omega H \qquad (7)$$

Although B is not necessarily the same on both sides of the interface, H and ω are the same, hence $\rho/\mu\ dG/dr$ is continuous at the interface.

The solution[6] of equation 3 in each of the regions is

$$G_{\text{I}}=C_1I_0(j^{1/2}ar)+C_2K_0(j^{1/2}ar)$$

$$G_{\text{II}}+C_3I_0(j^{1/2}br)+C_4K_0(j^{1/2}br) \qquad (8)$$

in which the C_1, C_2, C_3, and C_4 are constants to be determined, and $I_0(j^{1/2}ar)$, $K_0(j^{1/2}ar)$, and so forth, are modified Bessel functions of the first and second kind respectively.

By the first boundary condition $C_2=0$. The other two conditions permit the evaluation of C_3 and C_4 in terms of C_1 which, since $I_0(0)=1$, is the current density at the center. There results

$$G_{\text{I}}=C_1I_o(j^{1/2}ar) \qquad (9)$$

$$G_{\text{II}}=C_1j^{1/2}br_1\frac{\rho_1}{\rho_2}I_o(j^{1/2}ar_1)K_1(j^{1/2}br_1)N(br) \qquad (10)$$

where $N(br)$ has been introduced as the abbreviation

$$N(br)=I_0(j^{1/2}br)+\frac{I_1(j^{1/2}br_1)}{K_1(j^{1/2}br_1)}K_0(j^{1/2}br)+\frac{b\rho_2K_0(j^{1/2}br_1)I_1(j^{1/2}ar_1)}{a\rho_1K_1(j^{1/2}br_1)I_0(j^{1/2}ar_1)}I_0(j^{1/2}br)-\frac{b\rho_2I_1(j^{1/2}ar_1)I_0(j^{1/2}br_1)}{a\rho_1I_0(j^{1/2}ar_1)K_1(j^{1/2}br_1)}K_0(j^{1/2}br) \qquad (11)$$

The current density at the interface is obtained by substitution of r_1 for r in $N(br)$ as it occurs in equations 10 and 11,

$$G_1=C_1j^{1/2}br_1\frac{\rho_1}{\rho_2}I_0(j^{1/2}ar_1)K_1(j^{1/2}br_1)N(br_1) \qquad (12)$$

Similarly, at the outer surface where $r=r_2$

$$G_2=C_1j^{1/2}br_1\frac{\rho_1}{\rho_2}I_0(j^{1/2}ar_1)K_1(j^{1/2}br_1)N(br_2) \qquad (13)$$

The total current is

$$I=2\pi\int^{r_1}G r_{\text{I}}dr+2\pi\int_{r_1}^{r_2}G r_{\text{II}}dr \qquad (14)$$

which, upon substitution of equations 9 and 10, integration, and simplification, gives

$$I=2\pi C_1r_1r_2\frac{\rho_1}{\rho_2}I_0(j^{1/2}ar_1)K_1(j^{1/2}br_1)D(br_2) \qquad (15)$$

where $D(br)$ is the abbreviation

$$D(br)=I_1(j^{1/2}br)-\frac{I_1(j^{1/2}br_1)}{K_1(j^{1/2}br_2)}K_1(j^{1/2}br)+\frac{b\rho_2K_0(j^{1/2}br_1)I_1(j^{1/2}ar_1)}{a\rho_1K_1(j^{1/2}br_1)I_0(j^{1/2}ar_1)}I_1(j^{1/2}br)+\frac{b\rho_2I_1(j^{1/2}ar_1)I_0(j^{1/2}br_1)}{a\rho_1I_0(j^{1/2}ar_1)K_1(j^{1/2}br_1)}K_1(i^{1/2}br) \qquad (16)$$

The constant coefficients of $D(br)$ are the same, except for sign, as the corresponding coefficients of $N(br)$; also in $D(br)$ the variable Bessel functions I_1 and K_1 replace I_0 and K_0, respectively, in $N(br)$. $N(br_2)$ becomes the numerator, and $D(br_2)$ the denominator of the important factor in the expression for the resistance ratio.

In these manipulations the relation

$$I_0(j^{1/2}br)K_0'(j^{1/2}br)-I_0'(j^{1/2}br)K_0(j^{1/2}br)=-\frac{1}{j^{1/2}br} \qquad (17)$$

(in which the prime indicates differentiation of the function with respect to the argument) proves to be useful in reducing the number of terms.

The impedance per-unit length (the reactance originating in that part of the flux only which is interior to the conductor) is the voltage drop per-unit length along the surface b divided by the current[6] or

$$Z_{ac}=\frac{\rho_2G_2}{I}=\frac{j^{1/2}\rho_2b}{2\pi r_2}\frac{N(br_2)}{D(br_2)} \qquad (18)$$

The d-c resistance is the net resistance of core and tube in parallel, and its reciprocal is

$$\frac{1}{R_{dc}}=\frac{\pi r_2^2}{\rho_2}\left[1-\frac{r_1^2}{r_2^2}\left(1-\frac{\rho_2}{\rho_1}\right)\right]=\frac{\pi r_2^2}{\rho_2}P \qquad (19)$$

where P is the per-unit conductivity, the ratio of the conductance of the actual conductor to that of a solid homogeneous conductor of radius r_2 and resistivity of the same length. Thus

$$\frac{Z_{ac}}{R_{dc}}=j^{1/2}\frac{br_2}{2}P\frac{N(br_2)}{D(br_2)} \qquad (20)$$

The Bessel functions used in these equations are given in various forms in different tables.[3, 6, 7] The forms used in the present derivation are related to other forms in Table I. The Jahnke-Emde tables, which give the forms $J_0(j^{1/2}Z)$, $j^{1/2}J_1(j^{1/2}Z)$, $H_0^1(j^{1/2}Z)$, $j^{1/2}H_1^1(j^{1/2}Z)$, were found convenient to use, because both Cartesian and polar forms were given, and because less interpolation was required than in other tables that were available. Equation 1 gives the resistance ratio in terms of these forms.

Appendix B. Experimental Determination of Resistance Ratio

The ratio of a-c to d-c resistance of the specimen of copper-covered steel conductor was measured by means of the bridge circuit shown in Figure 9. The bridge, similar to that used by Kennelly and Affel[8] contains resistance in two arms, and both resistance and inductance in the other two. The bridge is arranged for either alternating or direct current.

R_1 and R_2 are matched resistances of 100 ohms each. L_3 is a toroidal fixed inductance of 35 microhenrys. L_4 is a variable inductor, with a maximum inductance of 36 microhenrys. These inductances are designed to balance the bridge with the unknown in and out of the circuit. The conductor under test has an inductance of 25 microhenrys. R_s is a Manganin slide wire, selected to have negligible skin effect for the range of frequency used, and r is an auxiliary resistance for adjusting the position of the sliding contact on R_s for balance at different frequencies. The conductor tested is switched out of the bridge by a copper bar resting in mercury cups.

The specimen of conductor under test, number 10 American wire gauge copper-covered steel of nominal 40 per cent conductivity, is mounted on an upright wooden frame six by six feet in size. The frame holds 77 feet of wire strung up and down with a spacing six inches between traverses.

The source of alternating current, a laboratory oscillator, is connected through a shielded transformer which serves to isolate the generator and to match its output to the relatively low impedance of the bridge. The alternating current through the unknown under test conditions varied between 0.04 and 0.16 ampere over the frequency range from zero to 160 kilocycles. The a-c null detector is a General Radio-wave analyzer, the frequency range of which was changed by removing condensers. An external amplifier is used to increase the sensitivity of the detector. For d-c balance a battery and galvanometer are employed. The experimental work was done in a closed room where the temperature was 29 degrees centigrade and remained constant to $^1/_2$ degree centigrade.

Measurements are made in the following manner: with the bridge unbalanced and the unknown in the circuit, a given frequency is applied to the bridge. Then L_4 and the sliding contact of R_8 are changed alternately to obtain a closer balance, and the sensitivity of the detector is increased progressively. When both an inductive and a resistive balance are obtained, the setting of the sliding contact on R_8 is noted. This is expressed as the distance l_1 of the contact from a reference point. Then this procedure is repeated for the same frequency with the unknown disconnected from the bridge, and the corresponding reading l_2 is noted. The resistance of the unknown at the given frequency is[8]

$$k(l_1-l_2)$$

where k is a constant which includes the resistance per-unit length of the slide wire. The bridge is balanced immediately then on direct current, and the difference of the two readings is noted. The ratio of the difference of the two a-c readings to the difference of the two d-c readings is the ratio R_{ac}/R_{dc} for the unknown at a given frequency.

Features which tend to minimize errors in the experimental method are:

1. All the components which are varied during a given measurement are placed in the same arm of the bridge. Thus, since each measurement involves a balance, once with the unknown in the circuit and once with the unknown out, the residual errors in the three unaltered arms cancel.

2. Coaxial connections to the various components are used to minimize stray magnetic coupling. Also a coaxial line is used in the detector circuit, for which purpose the single unit containing R_1 and R_2 is mounted on the sliding contact arm of R_8.

3. Components are placed far enough apart to minimize the effects of stray fields.

4. The fixed inductance L_8 and the variable inductance L_4 are made with no metal in their supporting structures. In the variable inductor L_4 litz wire is employed and the shaft, dial assembly, screws, and so forth, are all nonmetallic.

The over-all accuracy of the experimental method was checked by using it to measure the resistance ratio for ordinary copper wire. Values were obtained which agree with the known published values to an accuracy of one tenth of one per cent over the whole range of frequency, and this figure is taken as indicating the order of accuracy of the method.

The results for the copper-covered steel conductor are shown in Figure 2.

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A New 50,000-Kva 5-Kv Oilless Circuit-Breaker and Metal-Clad Switchgear Unit

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50,000-Kva Oilless Circuit Breaker

THE obvious advantages of air circuit breakers have led to their widespread application, particularly in the 2,500- and 5,000-volt class for 100,000- and 150,000-kva interrupting rating. This has in turn brought a demand for lower kilovolt-ampere ratings in these voltage classes. In the past the oil circuit breaker has been the only device economically applicable in this particular field. However, progress in design and manufacturing facilities has made it possible to build a 50,000-kva unit lower in cost than would have been considered feasible two years ago and one which is expected to meet the operating requirements in this field.

The interrupter is of the magnetic De-ion type and is illustrated schematically in Figure 1. Briefly, the theory of operation of this type of breaker embodies the use of spaced insulating plates of nongas-forming material having tapered slots into which the arc is moved by a magnetic field. As the arc moves into the narrow portion of the slot, electrons in the arc stream, attaining a high velocity from the magnetic field, bombard the gas particles of the arc stream and require the arc continuously to ionize fresh gas in considerable quantities. When current zero is reached, this action continues sufficiently to deionize the plurality of short lengths of arc near the edges of the plates, thereby establishing sufficient dielectric strength to interrupt the circuit.

In the design of the 50,000-kva breaker the insulating plates are made of a high heat-shock resisting material as in previous designs.[1,2] On account of the lower short-circuit currents for this rating, it was possible to reduce the thickness and width of these plates to two-thirds that of the higher interrupting ratings. For this reason also it was possible to space the plates about 30 per cent closer together.

In order further to reduce the size of the arc chute, the number of plates was reduced. The required volts-per-plate interrupting ability was then obtained by increasing the magnetic field. In doing this, the number of turns on the blow-in coil was increased from 6 to 18. This gave a considerable increase in the magnetic field which in turn increased the voltage per plate.

This combination of design features sufficiently reduced the size of the arc chamber to permit the over-all breaker design to be comparable in size with the corresponding oil breaker.

Figure 2 illustrates the breaker as built for metal-clad use. It will be noted that the contacts represent a departure from previous practice on small air circuit breakers in that they are of the parallel blade type, resembling disconnecting switch construction. The hinged joint at the bottom of the blades is of the spring-pressure type with silver surfaces.

The main contact surfaces of the blades are of a type previously developed for disconnecting switches and have proved very reliable in that service. The main contact surfaces are integral with the blades, and hence joints between these parts are eliminated.

The movable contact blades are operated through a simple linkage by a

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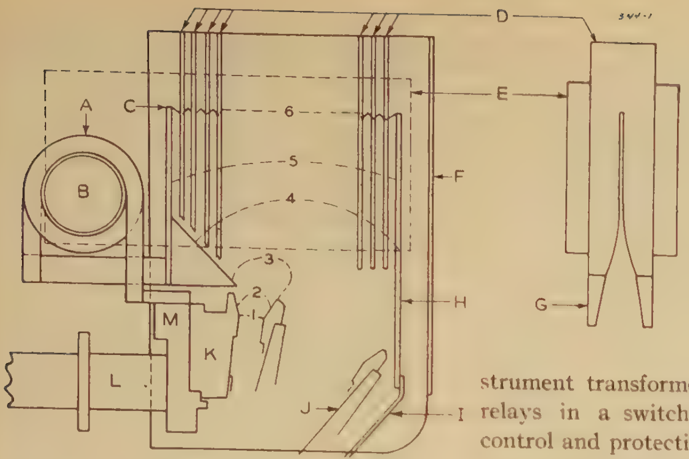


Figure 1. Schematic illustration of typical magnetic De-ion interrupter

The dotted lines 1, 2, 3, 4, 5, and 6 indicate progressive positions of arc interruption

A—Magnet coil
B—Iron yoke
C—Panel-end horn
D—Insulation plates
E—Laminated iron shoes
F—Outline of arc chamber
G—Arc shield
H—Arcing horn
I—Shunt strap
J—Moving contact arm and arcing contact
K—Arcing and secondary-contact platform
L—Micarta bushing
M—Upper stud

mechanically trip-free solenoid mechanism previously applied to oil breakers. Thus, the control circuits may follow established practice in all respects.

The unit illustrated in Figure 2A and 2B is designed to be rolled into the operating and test positions on wheels located on the main frame of the breaker only a few inches below the disconnecting contacts. This gives a close mechanical relationship between the disconnecting contact load, the drive-in force, and the load on the rails.

Tests

In line with modern switchgear-design practice where high-power laboratory facilities are available, numerous interrupting tests were made in the development of the breaker. Table I shows representative results of a typical series of three-phase tests on the breaker in its final form. It will be noted that the maximum kilovolt-amperes interrupted is approximately 60,000 at 4,800 volts, three phase. Figure 3 shows a typical oscillogram taken at 7,300 amperes and 4,800 volts, three phase.

The interruption of low currents at low power factor, corresponding to magnetizing currents, was investigated. The arcing time of the breaker extends to approximately 30 cycles on account of the low magnetic forces for moving the arc from the contacts into the arc chute. Since the arc chute is mounted directly above the breaker contacts, convection air currents assist in moving the arc, and this results in positive and consistent operation. Figure 4 shows a typical oscillogram taken at 20 amperes and 4,100 volts on a single pole.

Metal-Clad Switchgear Unit

To utilize the new 50,000-kva oilless breaker, a new horizontal drawout metal-clad switchgear unit has been designed. It incorporates the associated bus, instrument transformers, instruments, and relays in a switching structure for the control and protection of power distribution of circuits and associated electric apparatus. Figure 6 shows the air-breaker unit on the transfer truck in position to be placed in the housing.

Design Specification

In designing such a unit the following essential requirements had to be met:

(*a*). Compact design, still retaining best performance of circuit breaker under normal current-carrying and interrupting duty and mounting security.

(*b*). Safety and convenience in installation and operation.

(*c*). Accessibility for maintenance.

(*d*). Simplicity and minimum cost.

The metal-clad structure design should be such as not to reduce the effectiveness of the circuit breaker as measured by its performance unenclosed. To accomplish this, clearances and ventilation must be adequate and the arc gases freely vented. The mounting for the circuit breaker must secure it accurately and rigidly in its operating position to insure correct alignment of disconnecting devices and to prevent undue vibration and movement in service.

The horizontal drawout mounting is particularly adapted to air circuit breakers, since it permits a horizontal contact break and utilizes the definite tendency of the arc to rise and thus move naturally into the deionizing chamber. It also gives more ready accessibility to the breaker to permit quick superficial inspection.

All live parts should be enclosed in

Table I. Interrupting Tests on 50,000-Kva Air Breaker

Test Voltage	Current Interrupted				Megavolt-Amperes	Operation
	Phase 1	Phase 2	Phase 3	Average		
4,800	6,000	7,900	8,100	7,300	60.8	*O*
4,800	6,600	7,900	7,200	7,200	60.0	*O*
4,800	6,700	7,700	6,700	7,000	58.2	*O*
4,800	6,900	7,700	9,700	8,100	58.2	*CO*
4,150	8,600	6,800	6,800	7,400	53.2	*CO*
4,160	6,200	7,700	7,900	7,300	60.8	*CO*
4,800	7,900	5,800	6,800	6,800	56.6	*CO*
4,800	6,000	6,200	7,900	6,700	55.8	*CO*
4,700	2,400	1,700	1,900	2,000	16.6	*CO*
4,800	2,100	1,700	1,800	1,900	15.8	*CO*
4,800	490	430	580	470	3.9	*CO*
4,800	540	360	480	460	3.8	*CO*
4,800	1,030	1,010	970	1,000	8.3	*CO*
4,800	2,000	2,200	2,200	2,100	17.5	*CO*
2,200	17,400	12,600	18,700	16,200	61.8	*CO*
2,200	18,500	13,100	15,500	15,700	60.0	*CO*
2,200	16,200	12,100	17,300	15,200	58.0	*CO*
2,200	16,200	12,600	17,300	15,400	58.8	*CO*
2,200	17,400	14,400	14,100	15,300	58.2	*CO*
5,000	4,700	5,100	4,700	4,800	41.6	*CO*
5,000	5,200	3,800	5,300	4,800	41.6	*CO*

O—Open.
CO—Close-open

Table II. Interrupting Tests on 50,000-Kva Breaker Assembled in Metal-Clad Switchgear Unit

Test Voltage	Current Interrupted				Megavolt-Amperes	Operation
	Phase 1	Phase 2	Phase 3	Average		
5,000	5,515	5,890	6,495	5,967	51.6	*CO*
5,000	5,290	6,240	6,940	6,217	53.9	*CO*
5,000	5,890	6,450	7,710	6,683	57.8	*CO*
5,000	7,530	5,760	5,850	6,380	55.2	*CO*
5,000	7,550	5,690	7,200	6,880	59.5	*CO*

CO—Close-open.

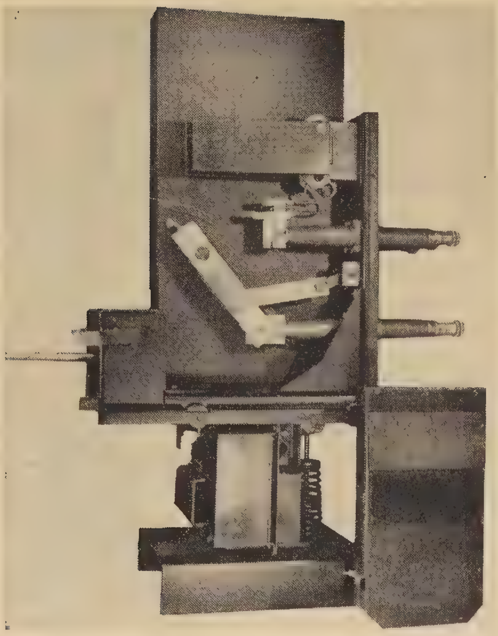


Figure 2A. Removable breaker for 600-ampere 5,000-volt 50,000-kva metal-clad switchgear unit

One arc chute shown removed at the lower right

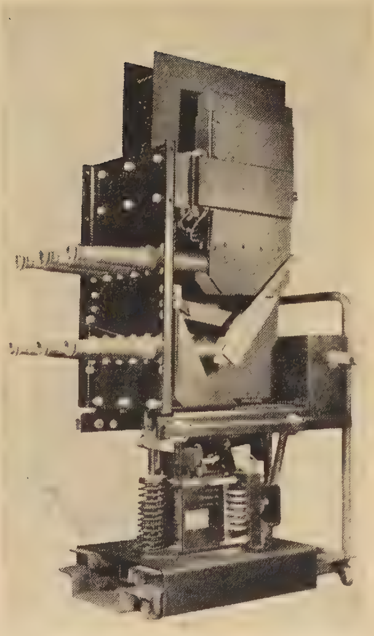


Figure 2B. Rear view of removable air-circuit-breaker unit

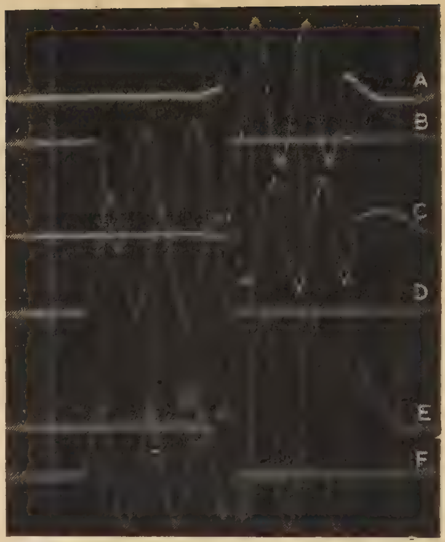


Figure 3. Oscillogram showing interruption of 7,300 amperes at 4,800 volts, three phase, 60 cycles on the 50,000-kva oilless circuit breaker

A—Voltage, phase 3
B—Current, phase 3
C—Voltage, phase 2
D—Current, phase 2
E—Voltage, phase 1
F—Current, phase 1

grounded metal compartments, and covers should be provided to give access to each portion of the circuit separately, without exposing any other circuit. The structure must be built ruggedly to withstand shipping stresses and to permit handling as a complete assembly without causing misalignment, so that it can be shipped in as large unit groups as possible.

Operation should be simple, since the application for this size breaker is quite largely in small stations where highly trained personnel is not available.

General Description of Metal-Clad Unit

The steel housing consists of a welded structural frame with sheet-steel barriers dividing the unit into three principal high-voltage compartments for the bus, circuit breaker, and current transformers, together with the main cable terminals. The complete units are separated from each other by sheet-steel barriers, and the bus opening through this barrier is entirely covered by the Micarta insulating support in order to divide the bus compartment into unit lengths.

The busses and connections are insulated completely with Micarta and varnished cambric as shown in Figure 7. The main bus joints are insulated with molded enclosures, which are clamped over the joint by means of tapered keys, and are filled with insulating compound to seal the joints and make the bus insulation continuous. All joints in busses and connections are silver-surfaced to maintain permanently high conductivity.

The highly important primary disconnecting devices consist of flexible finger assemblies mounted directly on the ends of the removable breaker-unit bushings and cylindrical studs mounted in insulating tubes in the housing to provide insulation between phases and to shield them from accidental contact, as Figure 8 shows. The flexible finger assembly consists of bridging segments which make point contacts on the breaker bushing at one end and on the cell stud at the other end, when in the engaged position. A new form of segment is used which is profiled, so that each segment makes two points of contact at each end to reduce millivolt drop. High unit pressure is obtained by flat springs located between an encircling ring and the contact segments in order to exert radial pressure inward on each segment. It will be noted that since each spring exerts radial pressure on its own segment independently of the others, the contact pressure exerted is independent of the number of segments used, so that various contact capacities can be obtained by using different multiples of the same segments and springs. Contact surfaces are heavily silver-plated to insure permanently low resistance.

The hinged panels on the front provide space for mounting instruments, relays, control switches, and other panel devices to form a completely self-contained switching unit as shown in Figure 9. The control and secondary circuits are completed to the breaker unit by means of a multipoint plug and receptacle-type contact. Figure 10 shows the plug assembly on the breaker unit mounted on a novel sliding member with sufficient travel to permit re-engaging it for test when the breaker unit is moved to the test position with the main contacts disengaged. The contacts engage automatically in the operating position, and the sliding mounting is latched, so that they disengage

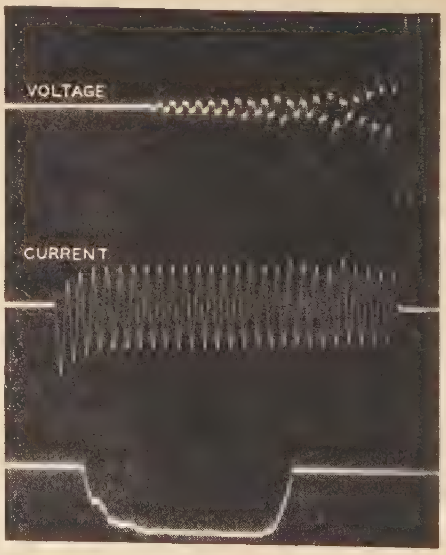
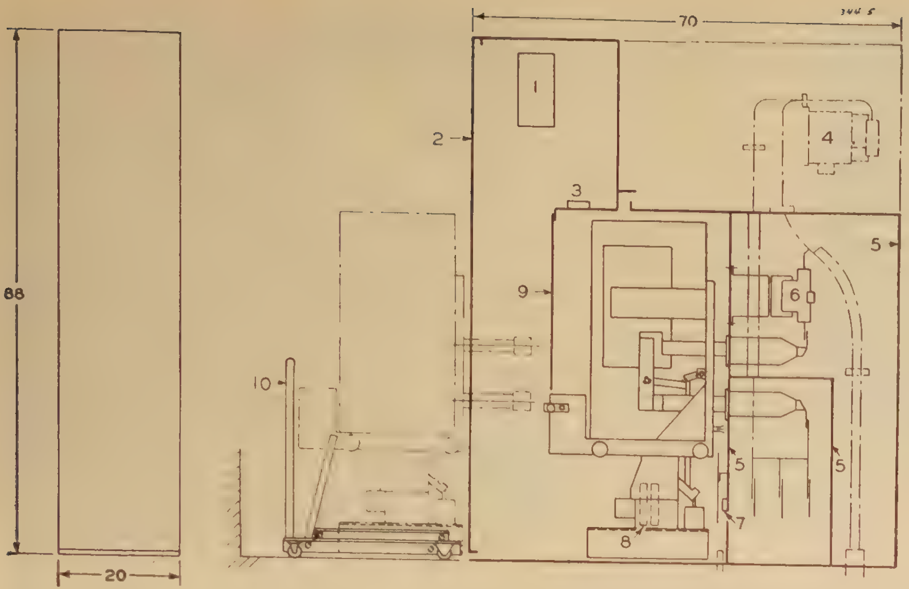


Figure 4. Oscillograms showing typical low-current low-power-factor interruption

Current 20 amperes at 4,100 volts across a single pole



A. Front view

B. Sectional side view

Figure 5 (above). Outline of metalclad unit

Front view and section drawing showing isolation between high-voltage compartments of housing

1. Control panel
2. Hinged door (instrument panel)
3. Cross wiring
4. Potential transformer
5. Removable cover
6. Current transformer
7. Terminal block
8. Secondary contact
9. Hinged barrier
10. Transfer truck

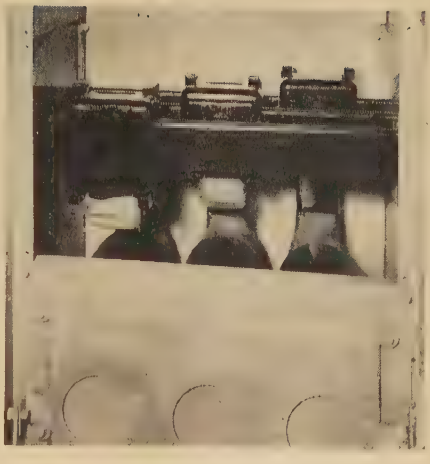


Figure 7. Insulated bus and connections assembly

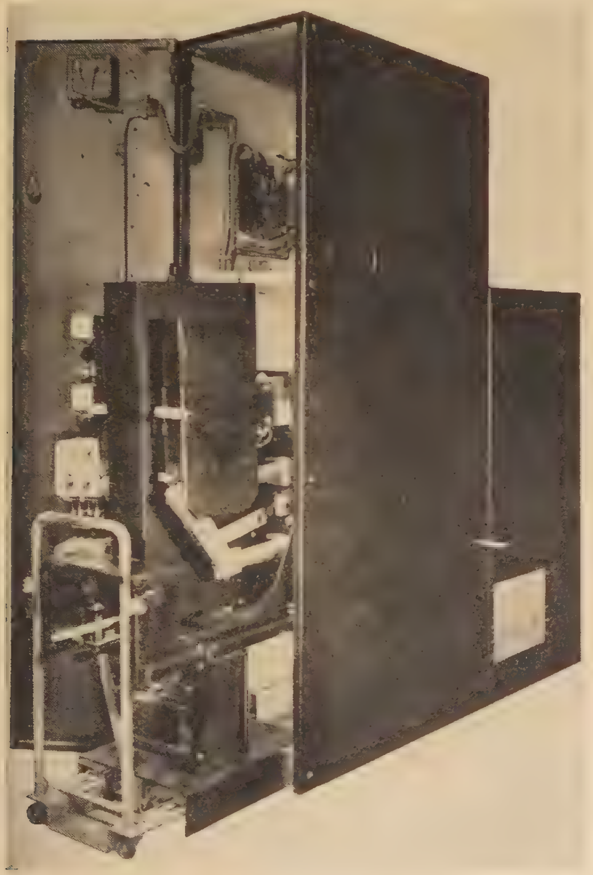


Figure 6 (left). Metal-clad switchgear housing with air circuit breaker on transfer truck in position to enter

Lever operated elevating device of transfer truck permits lowering unit to floor or picking up

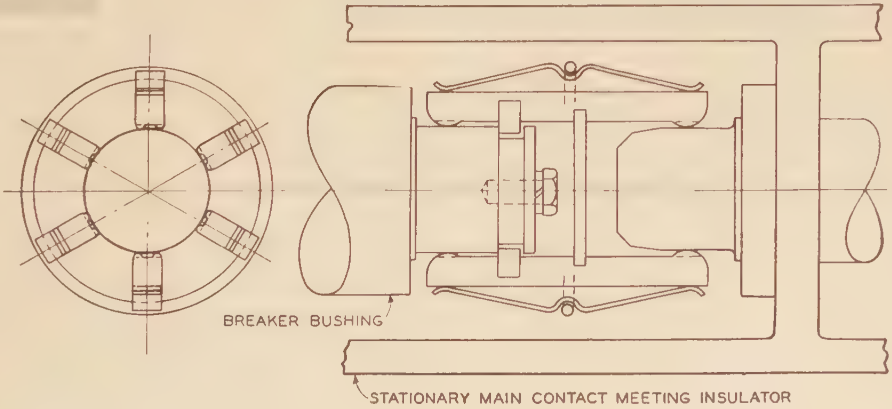
normally when the unit is withdrawn to the test position.

The removable unit, comprising the circuit breaker and its frame support, is carried on flanged rollers which engage the track in the housing. It is transported and placed in position by means of an auxiliary transfer truck equipped with a simple lever-type elevator of limited movement, which will raise the unit from the floor and lower it to position on the housing rails. From this point, a single rugged worm-and-gear levering device is used to move the breaker unit into and out of the operating position. The worm mechanism is self-locking and holds the removable unit securely in the operating position. A notched cam on the levering-device shaft prevents moving the breaker into or out of engagement with the disconnecting contacts, unless the breaker is in the open position, and permits breaker operation only in the operating and test positions. (See Figure 11.)

Tests on Assembled Switchgear

Supplementing the detail tests made on the separate circuit breaker, tests were made in the metal-clad housing at maximum interrupting rating. In addition, a series of repetitive moderate interrupting-duty tests was made to simulate typical motor-inching duty where starting current is opened and closed several times in rapid succession. High-current tests were made as shown in Table II. Twenty-five close–open tests with nine-second intervals were

Figure 8. Details of primary disconnecting contact showing both stationary and moving members



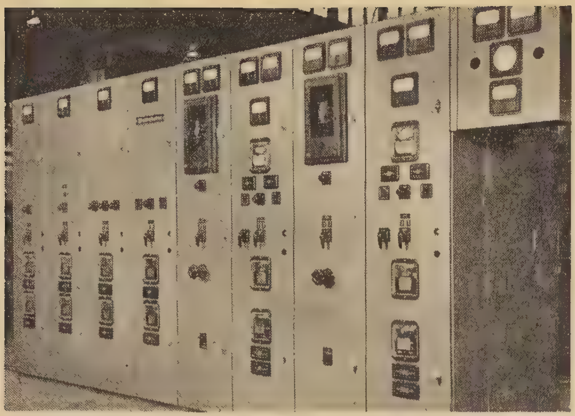


Figure 9 (left). A typical metal-clad assembly for control of two generator and four feeder circuits

Auxiliary sections are used to mount the voltage regulator and excitation control equipment

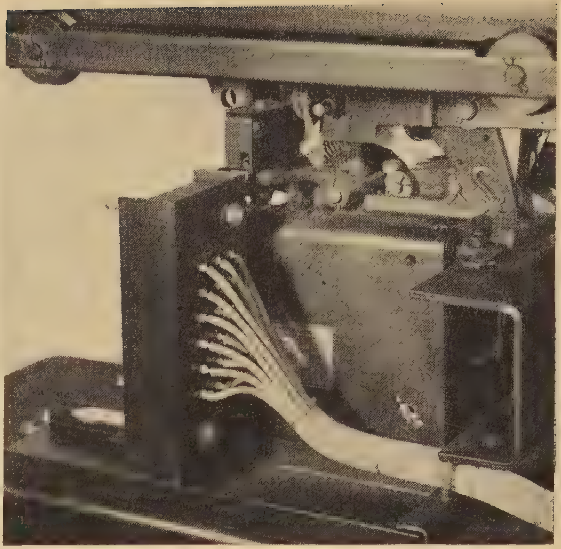


Figure 10 (right). Secondary contact assembly on removable unit with contact block mounted on sliding support to permit engaging control circuits with the breaker unit in the test position

made at approximately 1,500 amperes to simulate repetitive duty on typical motor-starting inrush current. These tests were made too rapidly to permit taking oscillographic records of current.

Careful examination and inspection during and after the tests showed that the enclosing housing has adequate clearance and ventilation to insure the best performance of the circuit breaker and that the connection and bus structures and breaker support are entirely adequate. Figure 11 shows the condition of the breaker contacts after the series of tests.

In addition, momentary current tests at the maximum short-time rating of 25,000 amperes were made on the assembled structure and circuit breaker to prove that the mechanical and thermal capacity was adequate. Dielectric tests at 19 kv, 60 cycles for one minute and 60 kv impulse were made in accordance with the proposed insulation levels for this class of equipment.

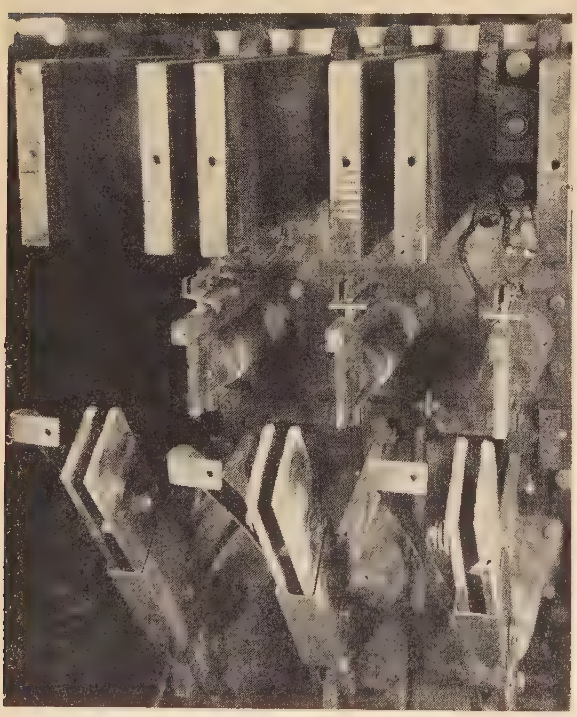


Figure 11. Breaker contacts after series of 73 close–open operations, 26 of which were at or above rated interrupting capacity

Conclusion

Extensive development work and careful cost-analysis work on the larger sizes of air circuit breakers have made it possible to produce a 50,000-kva unit comparable in size, weight, and use of critical material with the corresponding oil-circuit-breaker unit. This makes the obvious advantages in maintenance, safety, and convenience of air-type interrupting devices available for applications to smaller generating and distribution stations, industrial plants, and unit substations.

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1. A New High-Capacity air Breaker, L. R. Ludwig, G. G. Grissinger. AIEE Transactions, volume 58, 1939, August section, pages 414–20.

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Factors Affecting the Design of D-C Magnets

L. T. RADER
ASSOCIATE AIEE

Synopsis: This paper outlines some of the factors which should be considered in the design of d-c magnets. The usual types of steel used are briefly discussed, including their influence on "sticking" or residual forces. Calculated curves are presented which show the influence of pole-face area on the force developed by the magnet. Flux measurements on a number of magnets show that theoretical calculations are often in great error because of factors such as leakage and saturation which are usually neglected. Data show that the flux in a d-c magnet with a small air gap may vary by as much as 300 per cent between one part of the circuit and another.

D-C magnet design is of tremendous importance today because of the almost exclusive use of d-c power in mobile equipment such as tanks and airplanes. Magnets are the backbone of the d-c control systems, for, when energized by push buttons or interlocks, they furnish the power to operate contactors and relays of all descriptions or to perform strictly mechanical functions, such as actuating firing pins or locking devices. With applications such as these, new requirements have arisen. Chief of these are light weight, minimum volume, insensitivity to vibration with the magnet open or closed, and higher operating temperatures.

Although d-c magnet design is an old subject, there appears to be very little literature on it which is of help to the designer. Text books[1] give an adequate presentation of theoretical principles involved, but assumptions which must be made to solve a problem theoretically are more often than not the dominant considerations in practical design. The tremendous diversity of design requirements has led to a large number of different forms of magnets dictated by device operation and requirements. It is impossible to find a design procedure suitable for all cases, but the same factors, such as saturation, leakage, residual forces, and kind of steel, must be considered in each. Of these, leakage appears to bear an importance not previously suspected. Flux measurements have been taken on a large number of magnets to show their magnitude. The results of some of these measurements are discussed.

Steels

At present, the four general kinds of steel which are considered for use in the usual design of d-c magnets are:

1. Cold rolled or stamping steel.
2. Silicon steel.
3. Low metalloid steel or ingot iron.
4. High-saturation steel such as 50 per cent cobalt, 50 per cent iron.

Tables I and II list the magnetic properties of these steels. The term, "aging factor," which appears in Table I is defined as the per cent increase in hysteresis loss which occurs to a steel with time. As applied to d-c magnet design, an increase in the hysteresis-loop area is significant principally in the second quadrant, where an increased area means an increase in external energy and in coercive force. An accepted laboratory method of finding the aging factor is to measure the d-c hysteresis loop before and after holding the steel at 100 degrees centigrade for 600 hours.

Common stamping steel is the most widely used for d-c magnets because of its low cost and its desirable mechanical qualities. It is readily drawn, punched, or formed, and is available in a large variety of forms and sizes. Its main disadvantage is that it is not sold as a magnetic steel; the mills do not guarantee magnetic qualities. Large variations, therefore, can and do occur in the magnetic quality of this steel. In general, in the annealed condition, it has a better magnetization curve for the higher densities and a higher saturation value than the silicon steels. However, its coercive force is high; its available external energy is high, and its aging factor is large. A design using this material must be checked carefully, not only for residual forces which exist at the time of manufacture, but also for increased forces which may develop because of aging.

As the percentage of silicon in steel increases, the saturation curve becomes poorer, and the intrinsic saturation falls. The silicon steels are also harder to fabricate. These disadvantages are offset by the fact that these steels are sold with specified magnetic properties and are of consistent quality. As the silicon content increases, the aging factor decreases, the largest aging factor for a silicon steel being considerably less than for common stamping steel. The coercive force and external energy are likewise considerably less.

Ingot or Armco iron is the purest form of iron sold commercially. When properly

Table I. Magnetic Properties of D-C Magnet Steels

Kind of Steel	Alloying Constituent (Per Cent)	Intrinsic Saturation (Kilolines Per Square Inch)	Aging Factor (Per Cent)	Residual Induction (Kilo-gausses)	Coercive Force (Oersteds)	External Energy (Maximum)
Common	0.25 silicon maximum	133	15	7.9	1.5	4,650
Silicon *A*	0.25-0.75 silicon	132.3	8	8.37	0.86	3,450
Silicon *B*	0.75-1.25 silicon	131.5	5	8.0	0.70	2,400
Silicon *C*	2.50-3.0 silicon	126	3	8.1	0.67	2,450
Ingot iron	None	135.5	Varies	7.5	1.05	2,330
Cobalt	50 cobalt	148		16.5	3.5	25,800

Table II. Magnetization Data for D-C Magnet Steels

Kind of Steel	Total Flux Density (Kilolines Per Square Inch)												
	10	30	50	70	80	85	90	95	100	105	110	115	120
	Magnetizing Force (Ampere Turns Per Inch)												
Common	1.98	3.55	5.1	8.1	11.1	14.1	19.2	28	45	77	136	235	355
Silicon *A*	1.17	2.05	3.35	6.2	10.3	14.8	22.5	38	66	120	192	290	420
Silicon *B*	1.20	2.05	3.3	6.4	10.9	15.0	22.5	39	69	120	195	300	430
Silicon *C*	1.10	1.79	3.1	6.4	11.8	18.5	34	65	116	187	280	410	570
Ingot iron	1.2	2.3	3.5	5.2	6.6	7.5	8.9	12.0	21.5	55	115	165	257
Cobalt	2.6	4.4	5.3	6.0	6.3	6.6	6.9	7.4	8.1	9.0	10.5	12.5	15.8

Paper 43-47, recommended by the AIEE committee on electrical machinery for presentation at the AIEE national technical meeting, New York, N. Y., January 25-29, 1943. Manuscript submitted November 23, 1942; made available for printing December 21, 1942.

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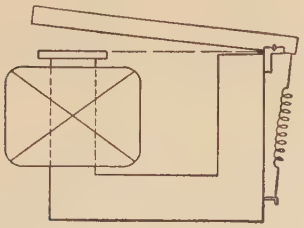
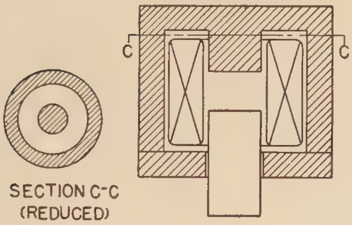
annealed, it has the best magnetization curve and the highest intrinsic saturation value of the usual steels. It is ductile and tough, but readily forged, machined, or welded. Its external energy curve is low. Its magnetic qualities tend to be influenced considerably by the type of anneal which it receives. Its main disadvantages are its relatively high cost and lack of availability in various forms and sizes.

The use of special high-saturation steels is not so widespread as might be supposed, because of the relative difficulty of obtaining them, together with their high cost, poor machining properties, and high coercive force and residual induction. However, as Tables I and II show, a 50 per cent cobalt, 50 per cent iron has a remarkably good induction curve and high intrinsic saturation.



A. Clapper or pivoted armature



B. Solenoid

Figure 1. Types of d-c magnets

In all d-c designs, especially in those using stamping steel, it is essential for consistent performance that an air gap be in the circuit between the armature and pole face. The order of magnitude of residual forces is often underestimated. In one case a magnetic circuit of ingot iron weighing five pounds two ounces was found to have 33 pounds residual force when there was no air gap in the circuit. In another case a small relay using annealed stamping steel weighing 32 grams had a measured residual force of 112 grams. When a magnet operates, the repetitive shock of one part on another, together with the simultaneous application of magnetic field, tends to increase the coercive force. It has been found in practice that this increase may be considerably more than the aging factor would indicate.

Classification of D-C Magnets

D-c magnets can be classified into two general groups:

1. Clapper or pivoted armature type.
2. Solenoid or pot type.

A typical form of clapper magnet is shown in Figure 1A. An armature is pivoted on one face of a U-shaped piece and rotates through an air gap to strike a pole face. The coil may be placed on any leg of the core. In Figure 1B is shown a solenoid-type magnet in which a plunger, usually circular, moves within a coil to strike a core or head finally.

Design requirements for a magnet are usually:

1. It should give a force-versus-stroke curve suitable for the particular requirement.
2. It should utilize to the fullest extent every ounce of iron and copper in the magnet.
3. It always should release under the action of the restoring spring when de-energized.

These requirements determine whether a clapper or solenoid magnet will be used. The shape of the pull curve of a clapper magnet can be changed by varying the pole-face area; that of a solenoid type by using some variation of a stepped plunger or conical plunger.

Force Formula

In order to find the flux in a circuit consisting of an iron path in series with a short air gap, a trial-and-error method is usually employed. A value of flux is assumed; the various magnetomotive-force drops are found, and their total compared to the applied magnetomotive force. A result sufficiently accurate can be obtained usually in two or three trials. When the flux is found, the force between faces is obtained from

$$F=\frac{B^2A}{8\pi} \qquad (1)$$

where F is the force in dynes, B the air gap flux-density in gausses, and A is the area in square centimeters. This formula is the one occurring most frequently in texts and is apt to be misleading when the assumptions on which it is based are neglected. When saturation in the iron is negligible, equation 1 reduces to

$$F=\frac{B^2A^2}{8\pi A}=\frac{\phi^2}{8\pi A}=\frac{(0.4\pi NI)^2A}{L^2} \qquad (2)$$

where ϕ is the total flux crossing the air gap, NI the ampere turns applied to the circuit, and L the length of the air-gap centimeters.

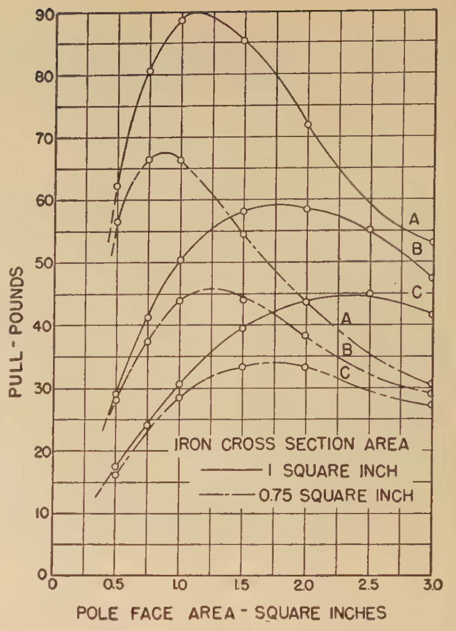


Figure 2. Effect of pole-face area on pull for various air gaps

A. 0.060-inch gap
B. 0.090-inch gap
C. 0.120-inch gap

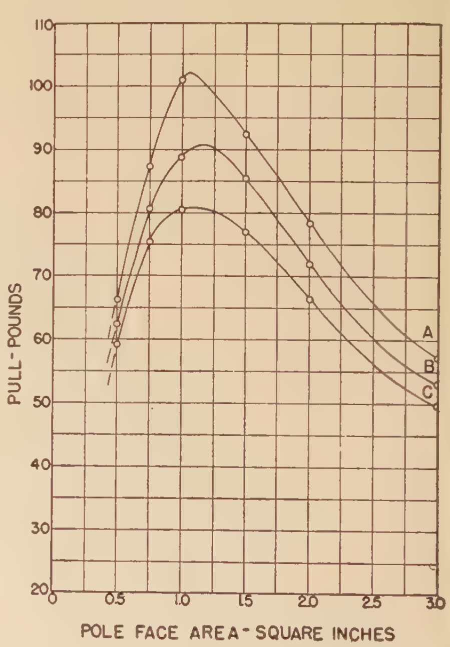


Figure 3. Effect of pole-face area on pull for various lengths of magnetic paths

A. 7.5 inches
B. 10.0 inches
C. 12.5 inches

Equation 2 is a much more accurate form to use when drawing conclusions of the effect on the force of changing physical dimensions. This equation states that if there is no magnetomotive-force drop in the iron, the force varies directly as the face area, directly as the magnetomotive force squared, and inversely as the air gap squared.

Optimum Pole-Face Area

It is seldom economical to operate a magnet so low in flux density that the magnetomotive-force drop in the iron can be neglected. In general, then, when saturation is taken into account, it is possible to show analytically that there is a pole-face area which will give the maximum pull for any one particular set of conditions. The method involves finding a suitable expression for the B, H curve, solving for B in the particular circuit, and then substituting for B^2 in the force equation. This equation may be differentiated then with respect to the pole-face area to find the area for maximum pull. This method is long and tedious. It is faster and more accurate, as soon as a little experience has been gained, to solve a circuit by a trial-and-error method, using the saturation curve of the steel involved. To illustrate the results obtained by this latter method, the following example was worked out: A magnetic circuit was assumed to consist of a ten-inch iron path in series with an air gap of 0.060 inch, 0.090 inch, and 0.120 inch with 2,000 ampere turns applied to the circuit. Two assumptions were made:

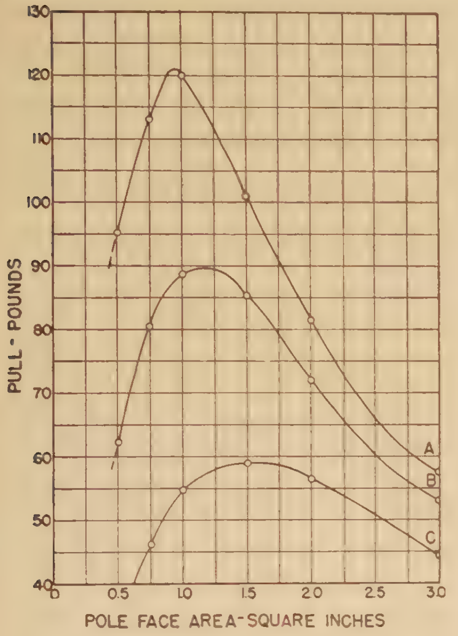


Figure 4. Effect of pole-face area on pull for various impressed magnetomotive forces

A. 2,500 ampere turns
B. 2,000 ampere turns
C. 1,500 ampere turns

1. The ampere turns per inch of iron circuit were constant.
2. There was no fringing in the air gap.

In view of data which are presented later, it will be seen that these two assumptions introduce large errors. They are, however, necessary for a theoretical solution which must be arrived at without the aid of any experimental data. Figure 2 shows the results obtained. It should be noted that the area for maximum pull varies for different air gaps. This point is of great importance for it means that each magnet design can be shaped for a particular requirement.

In the operation of d-c contactors it is a desirable characteristic to have the forces such that the armature will not hesitate when the contact tips first touch. By using the proper core head, the force generated can be kept low in the fully open position and made a maximum at the point where the contact forces begin, so that, when the armature once starts to move, it carries the tips through their kiss-and-wipe position without hesitation. Figure 3 shows the result of varying the length of iron path for the case when the iron cross section is one square inch, the applied ampere turns 2,000, and the air gap 0.060 inch. Figure 4 shows the effect of impressing various magnetomotive forces on a magnetic circuit of ten-inch iron length, 0.60-inch air gap, and one square inch cross section. As the applied magnetomotive force is increased, the pole-face area for maximum pull decreases.

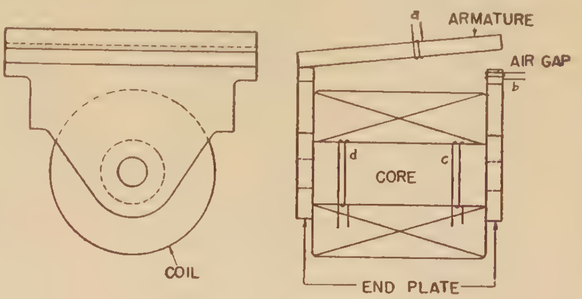


Figure 5. Form of d-c magnet used in studying flux variations

In certain devices such as telephone relays, where the flux density is often very low, the pull of a magnet follows equation 2. The size of the head or pole face can then be increased until the leakage, which is introduced because of the larger head, becomes a limiting factor. An example of this case using an equivalent electric-circuit analogy is treated in the literature.[2]

Air-Gap and Leakage Flux—Experimental Data

Figure 5 shows a type of clapper magnet whose proportions are to scale, which was investigated to obtain data on the distribution of flux in the circuit. Search coils consisting of five turns of 0.005*E* wire were wound on the frame and armature as shown at positions *a*, *b*, *c*, and *d*. The coil at *b* was wound very carefully as close to the air gap as possible and gave flux values which were taken as the air-gap flux. Data were taken on three such magnets, of the same proportions but of different sizes, which weighed about 8, 12, and 20 ounces. Various ampere turns were applied to the main coil; the flux in each of the search coils was then read for various air gaps. All flux data were taken in an engineering laboratory with a ballistic galvanometer. Using the measured values of density, the magnetomotive-force drop in the iron was found and subtracted from the applied magnetomotive force. The net magnetomotive force was used to calculate the flux which should cross the air gap, assuming no fringing.

The results obtained are shown in Figure 6 where *A*, *B*, and *C* represent the three different sizes of magnet. It should be noted that the equivalent fringing factor which may be taken as the ratio of measured-to-calculated flux is not constant but decreases with increasing density—probably because of local saturation at the edges of the pole face. For magnets *A* and *B* the pole-face thickness, which, as shown on Figure 5, is the end-

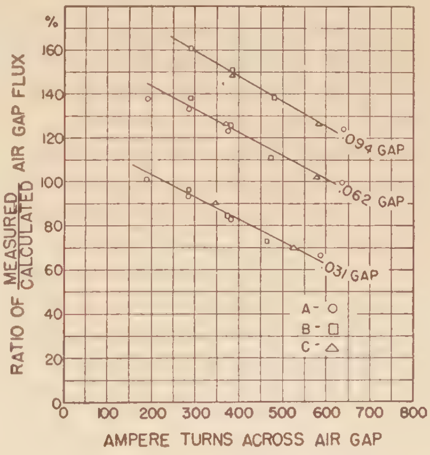


Figure 6. Ratio of measured to calculated air-gap flux for various impressed magnetomotive forces

plate thickness, was 0.094 inch. For magnet *C* the thickness was 0.125 inch. Because of the limited amount of data taken so far on this type of structure, no attempt has been made to determine fringing coefficients as a function of the air-gap dimensions. Figure 5, however, shows clearly the great error which would result if the air-gap flux is calculated on the basis of no fringing, even for the case where the separation is only 33 per cent of the pole-face thickness.

Table III shows the ratio of the total measured flux in the core (the average of search coils *c* and *d*) to the total measured flux in the air gap. In these three magnets the core was about 25 per cent larger in cross section than the rest of the magnetic circuit, but the density due to leakage is seen to be very much greater than this increased section can accommodate without excessive magnetomotive-force drop.

That these large leakage factors are not peculiar to the type of magnet illustrated in Figure 5 is shown from data which have been taken on other types. Readings obtained on a magnet similar to Figure 1A, except with a long thin coil and smaller separation between the legs of the core, showed a ratio of flux measured at the center of the coil to flux measured at the pole face of 250 per cent to 333 per cent over the operating gaps of the magnet. In a solenoid-type magnet, such as Figure 1B, the ratio of maximum flux in the circuit to the useful flux crossing the air gap was found to be 150 per cent.

These leakage figures, of course, vary with the dimensions employed. No attempt here is being made to compare the leakages for various constructions. The only fact which is being presented is that leakage flux in d-c magnetic devices is of an order of magnitude which makes it necessary that their effect be considered in the design. The often seen assumption, "neglect leakage," made in order to effect an analytical solution cannot be justified in many cases.

Conclusions

In addition to data presented here, extensive tests have been conducted on many types of d-c magnets from which the following conclusions may be drawn.

1. Residual forces in magnets may be very large. In a circuit without an air gap, these forces are dependent on the residual induction rather than on the coercive force. Since magnetic steels do not vary greatly in residual induction, an air gap must be in the magnetic circuit of even a low coercive-force material.

2. There is a best pole-face area for maximum pull. This area varies with the air gap and applied ampere turns.

3. Conventional methods of calculating flux in a magnetic circuit are likely to lead to large errors because of the lack of leakage coefficients in calculating air-gap flux.

4. The flux density in a conventional magnet with a small air gap may vary by as much as 300 per cent between one part of the circuit and another.

5. A ballistic galvanometer is a very useful tool in d-c design because of its accuracy and ease of use.

Table III. Ratio of Core Flux to Air-Gap Flux for D-C Magnets

Air-Gap Length in Inches	Magnet A	Magnet B	Magnet C
	Impressed Ampere Turns		
	300	400	500
	Core Flux / Air-Gap Flux in Per Cent		
0.031	190	183	166
0.062	236	224	200
0.094		253	225
0.252			306

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3. Electromagnetic Devices (book), **H. C. Roters.** John Wiley and Sons, Inc., New York, N. Y., 1941.

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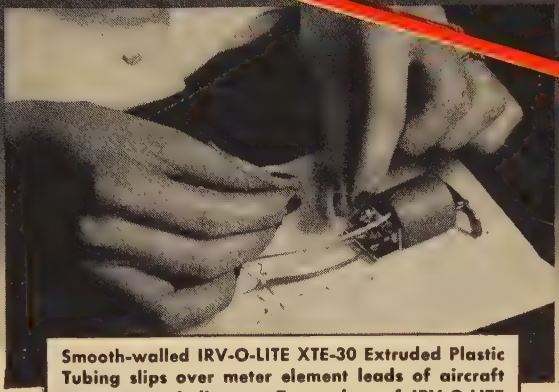

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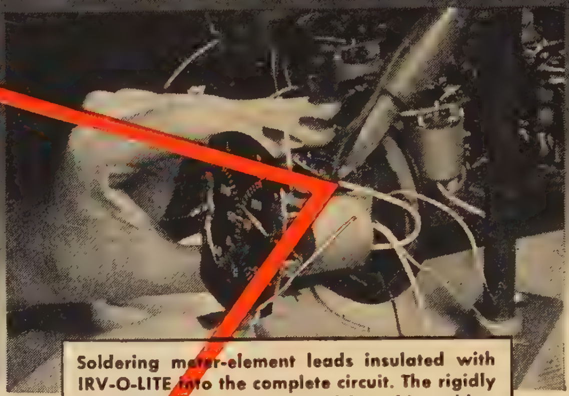

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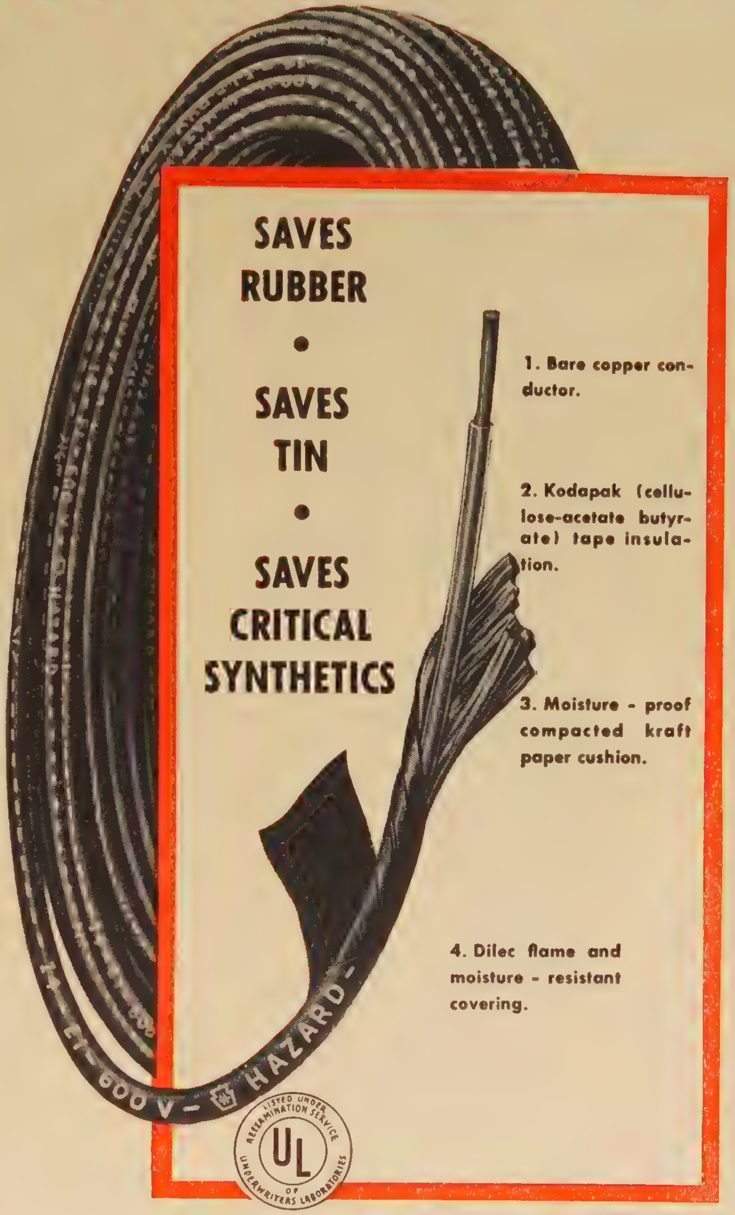
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HAZAPAK Rubberless Building Wire conductors are first insulated with a sealed layer of Kodapak synthetic tape over which is wrapped a further protective covering of moisture-proof, crumpled kraft paper. This insulation (full N.E.C. thickness), is then covered with a Dilec flame and moisture-resisting wrap. It consists of a cotton wrap with full coverage of cotton threads wound on spirally with several threads wound on in the opposite direction, binding the cotton tightly to the wire and forming a smooth, fully-covering sheath slightly smaller in diameter and smoother than a braid.

HAZAPAK Type EG Building Wire is approved as the neutral grounded conductor in common AC circuits and as the "white" conductor in cable assemblies such as Hazardex, armored cable, twin lead encased, etc., where the W.P.B. Rubber Restriction Order prohibits the use of rubber insulation on the white or grounded neutral conductor.

HAZAPAK Type EI Building Wire is approved for single conductor in open wiring as a wartime alternate for rubber-insulated wire, and as the "hot" wire in non-metallic sheathed cable that is run exposed in dry locations. It is available in any of the standard building wire colors.

The use of Types EG and EI HAZAPAK Rubberless Wire is covered by Interim Amendments Nos. 44 and 69 to the 1940 National Electrical Code.

Consult our nearest office for details about this new type building wire or ask your wholesaler about HAZAPAK.

ARMY E NAVY

HAZARD INSULATED WIRE WORKS

Division of The Okonite Co.

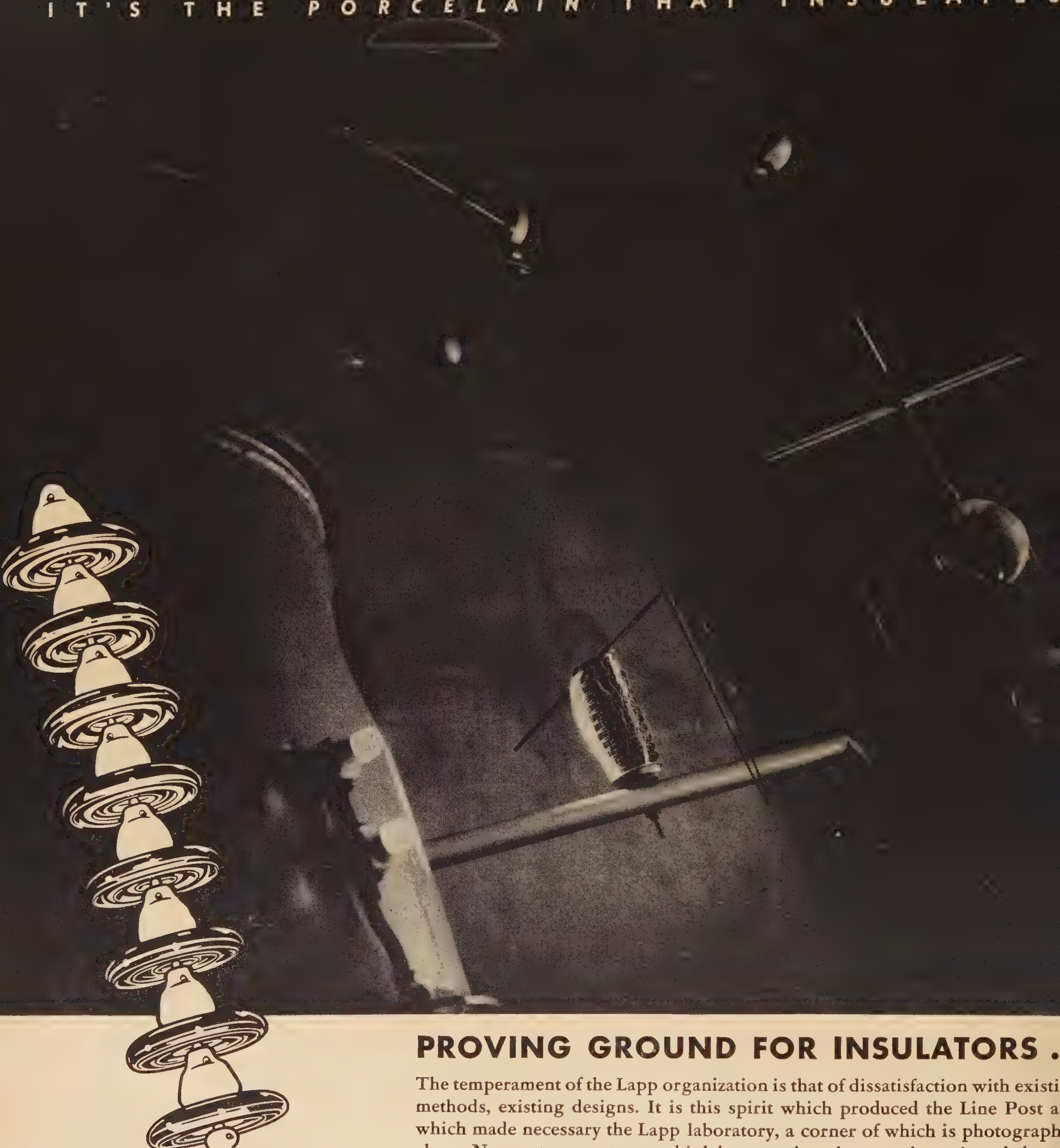
WILKES-BARRE, PA. Offices in Principal Cities.

HAZARD Electrical Wires and Cables

Salvage Your Scrap — Buy U. S. War Bonds

3324

I T ' S   T H E   P O R C E L A I N   T H A T   I N S U L A T E S



PROVING GROUND FOR INSULATORS .

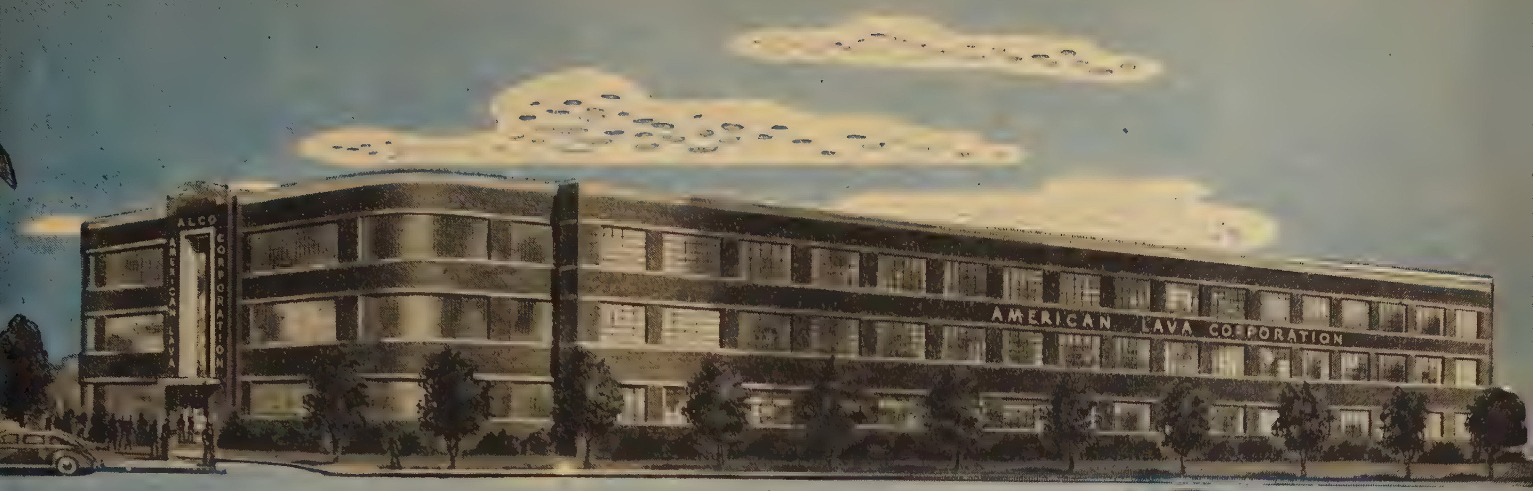
The temperament of the Lapp organization is that of dissatisfaction with existi
methods, existing designs. It is this spirit which produced the Line Post a
which made necessary the Lapp laboratory, a corner of which is photograph
above. No sanctum sanctorum, this laboratory is truly an engineers' workshop,
continuous day-to-day use, testing, discarding, proving new designs. As we lea
how to build better insulators, we build them. Meanwhile, we feel secure in c
claim that today's Lapp insulators are adequately suited to today's needs, t
you can do no better than to insulate your lines with Lapp. Lapp Insulator C
Inc., Le Roy, N. Y.

ARMY E NAVY

Lapp

Better Porcelain • Better Insulators

UT OF TODAY'S RESEARCH . . . TOMORROW IS ENGINEERED



ARMY E NAVY

AWARDED JULY 27, 1942

AN IMPORTANT ANNOUNCEMENT

FROM THE NATION'S LEADING PRODUCER
OF STEATITE CERAMIC INSULATORS

DUE TO TREMENDOUS EXPANSION IN CAPACITY AND PLENTIFUL RAW MATERIALS...THE STEATITE CERAMIC INDUSTRY CAN SUPPLY STEATITE INSULATION FOR ALL NEEDS.

THE TRADE CAN RELY ON STEATITE

SUBSTITUTE MATERIALS ARE NOT NEEDED

ALSIMAG
TRADE MARK REGISTERED U. S. PATENT OFFICE
STEATITE CERAMIC INSULATION
AMERICAN LAVA CORPORATION
CHATTANOOGA • TENNESSEE

Dunco High Efficiency
RELAYS FOR SMALL-SPACE USE

It's much more of a trick to make a fine, accurate wrist watch than it is to produce an alarm clock. Similarly, the task of making really dependable and durable midget relays is one that calls for the best that a specialized manufacturer, already accustomed to high standards of quality, can give it—and here Struthers Dunn engineering excels.

As a result, Dunco Midget Relays have established new standards of performance in a wide variety of applications where space is at a premium, and where dependability under exacting conditions of use is essential. They are produced in dozens of standard and special types and sizes for almost any requirement. Dunco Midgets are Underwriter approved.

OTHER DUNCO RELAY-TIMER TYPES

30 ampere — Sensitive — Instrument Controlled—Low Voltage, Heavy Current, D. C. — Mechanical Latch-in (Electrical Reset) — Mercury Contact—Telephone Auxiliary—Lamp Controlling — Polarized — Overload — Timing — Sequence, Ratchet Type—Motor Reversing, and many others.

Write for the New Dunco Catalog and Relay Data Book.

26 Dunco representatives located in every major war equipment manufacturing center throughout the U. S. and Canada are trained to help you in all problems of relay selection and use. Write or wire for address of your nearest representative.

STRUTHERS DUNN, Inc.

1321 ARCH STREET
PHILADELPHIA, PA.

NEED MOTORS OR GENERATORS?

If you need motors or generators, write or wire us . . . or consult your nearest Crocker-Wheeler office. We can make immediate deliveries on some types . . . but regardless of type, we can deliver in faster time than ever before.

New and vastly increased productive facilities, plus expert Joshua Hendy planning are the reasons. Crocker-Wheeler Electric Mfg. Co., Ampere, N. J. Division of JOSHUA HENDY IRON WORKS

Be **SURE** . . . with
CROCKER-WHEELER

Other JOSHUA HENDY IRON WORKS factories at Sunnyvale, Pomona and Torrance, California and St. Louis, Mo.

WHY *Casting* GIVES THE GREEN LIGHT TO CORRECT HARDWARE DESIGN

Casting is a flexible process—it imposes the fewest mechanical limitations—it allows closest adherance to true functional design
10 1/4"
1 1/16"
4 3/4 R
60°
1 3/16"
1 3/4"
9 3/8"

For the particular job of making line hardware, casting imposes the fewest design restrictions. Functional or theoretical shapes, proved or amended by long field experience, are not sacrificed because of factory production problems. That is why O-B casts its hardware. It is cast in malleable iron for the reasons briefly listed to the right... You needn't look far to find an O-B hardware user. Ask him about any phase of O-B design; ease of installation, strength, durability. You'll see there are some *real* reasons why O-B hardware should be your choice.

2422-H

Ohio Brass

MANSFIELD, OHIO

Canadian Ohio Brass Company, Ltd., Niagara Falls, Ont.

O-B Malleable Iron Hardware Is Today's Best Buy Because--

1. Malleable iron is shaped by casting which allows correct fuctional design to be incorporated with the fewest compromises brought about by manufacturing restrictions.
2. Malleable Iron is, fortunately, the least critical metal in the ferrous group--easiest to obtain for approved use.
3. Malleable iron has a good, practical ratio of strength to weight--O-B hardware is amply strong for the heaviest service.
4. Malleable iron, upon rusting, forms a tenacious, indestructible coating of iron-oxide that acts as a perfect protection for the metal underneath--it is unique in this valuable respect.
5. Malleable iron is extremely ductile--will withstand severe twisting and bending without failure.

★

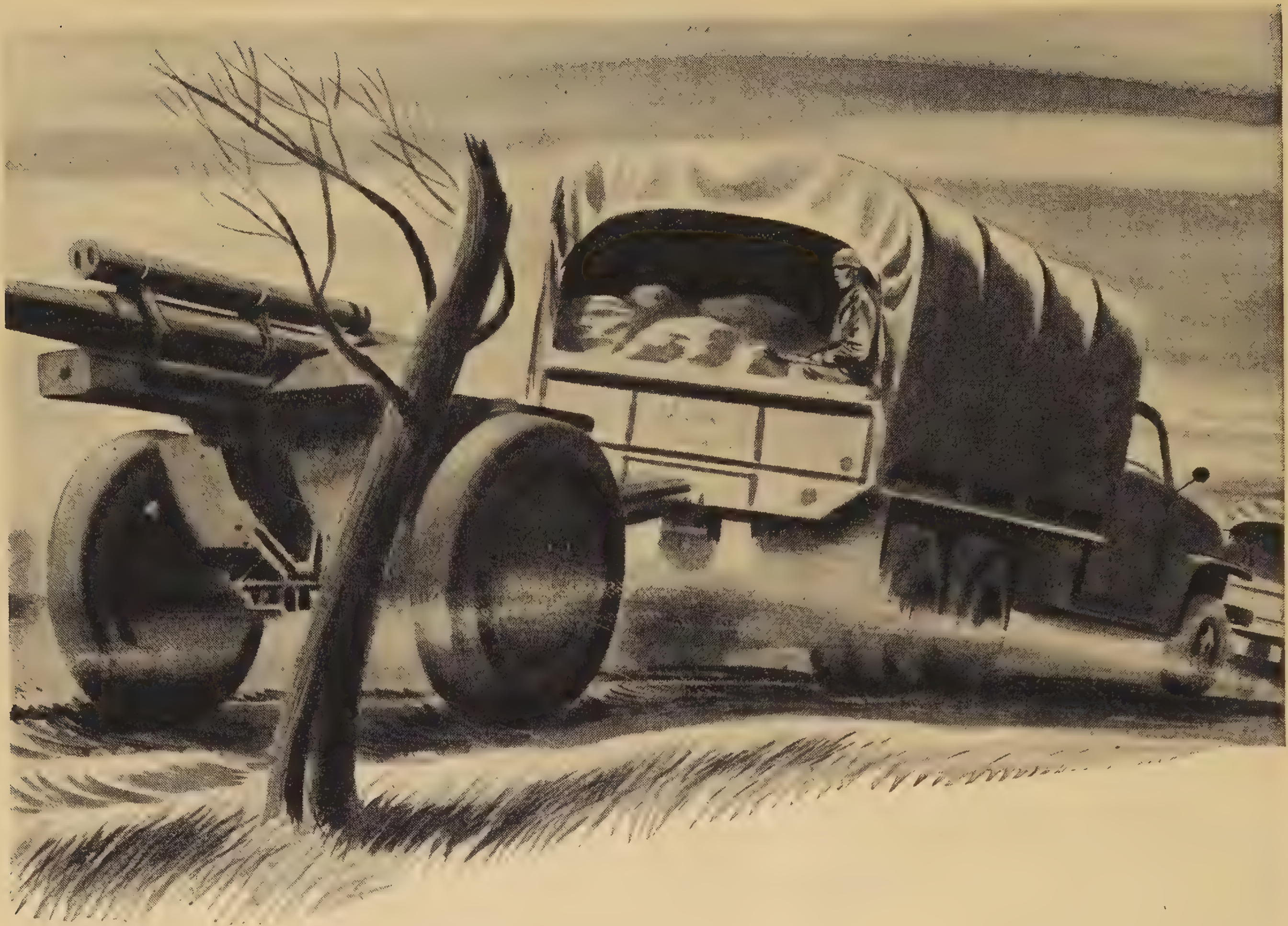
KEEP BUYING
UNITED STATES WAR BONDS

KERITE

in over three-quarters of a century of *continuous production,* has spun out a record of performance that is *unequalled* in the history of insulated wires and cables

1942
Another Year added *to the* Performance Record *of*
KERITE

THE KERITE INSULATED WIRE & CABLE COMPANY INC
NEW YORK CHICAGO SAN FRANCISCO



Speaking of superior races . . .

Every wheel that rolls on the battlefield turns in a polished bearing race, ruggedly built to take the terrific shock of combat service.

To withstand such punishment, bearing races must be hardened by heat-treatment. Hard and soft spots occasionally occur. Such races may fail—at times when failure means disaster.

Recognizing the vital need, Westinghouse Research Engineers set to work to develop a quick, sure method of detecting these flaws.

Their ingenious electromagnetic flaw-detector is based upon the fundamental law that the *permeability* of a heat-treated steel part varies with the degree of hardness.

In actual practice, the bearing race is *completely* demagnetized. Then it is rapidly rotated and strongly magnetized. While the race is still turning at high speed, its magnetic field is explored with a specially designed electromagnetic "pick-up."

Variations in the magnetic field of the bearing race, due to hard or soft spots, induce feeble currents in the pick-up system. These currents are amplified and shown visibly on a cathode-ray oscilloscope.

A uniformly heat-treated bearing race traces a *luminous straight line* on the oscilloscope screen. Faulty heat-treating shows up as a pattern of *hills and valleys.*

The electromagnetic flaw-detector is now being used commercially—a typical example of Westinghouse *electronics at work.*

It assures quality in millions of bearing races for our armed forces, to keep 'em rolling on to victory!

Westinghouse Electric & Manufacturing Company, Pittsburgh, Pennsylvania.

Electronic fingerprints — A Westinghouse Research Engineer demonstrates the principle of the electromagnetic flaw-detector. Hard spots in the steel test piece show up as an irregular line on the oscilloscope screen.

Westinghouse Electronics at Work

PLANTS IN 25 CITIES... OFFICES EVERYWHERE

HELPING AMERICA MEASURE UP

★ We can feel sure that the men who fight our battles are fully equal to the mighty task that faces them. ★ But the planes, tanks, ships, and guns they fight with must measure up, too. That's our job—those of us at home. How much? . . . How good? . . . are questions that only our hard work can answer. ★ Electrical instruments are a small but extremely vital part of America's war machine. Here at Simpson we are making *all* we can, the *best* we can, as *fast* as we can.

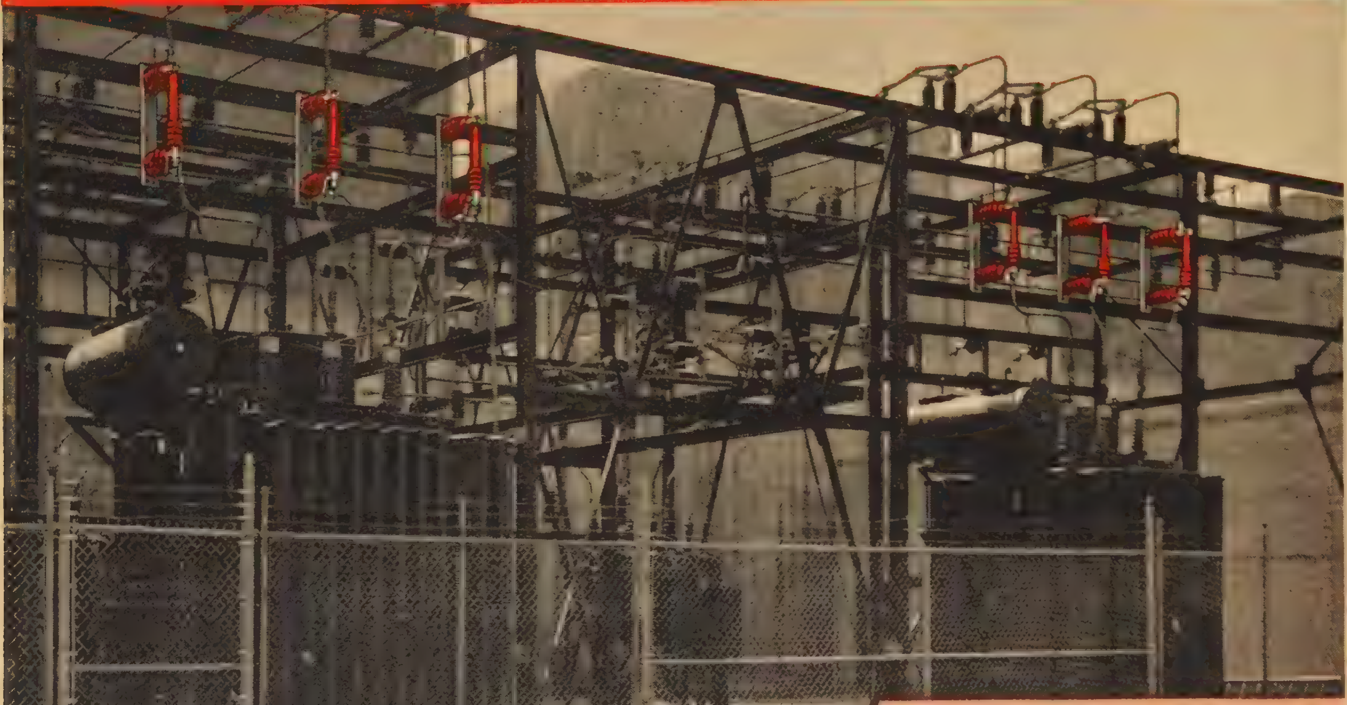
SIMPSON ELECTRIC COMPANY
5200-5218 W. Kinzie Street, Chicago, Illinois

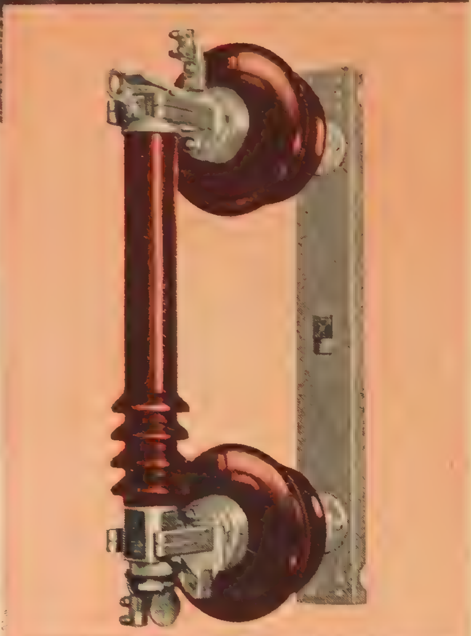
Simpson

INSTRUMENTS THAT STAY ACCURATE

Buy War Bonds and Stamps for Victory

PROTECTING VITAL PRODUCTION





SHORT CIRCUIT-INTERRUPTING RATINGS IN R.M.S. AMPERES

Fuse Voltage Rating →	* 7,500	7,500	15,000	23,000	34,500
Circuit Voltage →	2,300/4000	6,600	13,200	22,000	33,000
**SMPC-4	17,500	15,000	12,500	10,000	7,500
SMP-4	27,500	25,000	20,000	15,000	10,000
*SMPC-5	30,000	25,000	20,000	17,500	15,000
SMP-5	40,000	35,000	30,000	25,000	20,000

**Equipped with Condenser.

POWER supply and electrical equipment for the great plants producing vital armament and other war materiel require dependable protection to insure against production delays.

Such protection is being provided by S & C Type "SM" Power Fuses for many important installations. The one shown above on the property of a large aviation engine plant illustrates the small space required. Electrical engineers have selected these fuses because of their dependability, uniform accuracy in time-current characteristics and their high interrupting capacity. Their use also saves labor and installation time,—and they require the use of very little critical metals compared to other types of interrupting equipment of equivalent capacity.

High Interrupting Capacity

Within their ratings—S & C Type "SM" high interrupting capacity Fuses provide the ideal protection. They are made for indoor or outdoor service for 2300/4000, 6600, 13,200, 22,000, and 33,000 volt circuits in ampere capacities from 1 to 400, with short circuit interrupting ratings up to one millon KVA.

Recommendations by S & C engineers will help you select appropriate protective equipment.

SCHWEITZER & CONRAD, INC.
4435 Ravenswood Avenue, Chicago, Illinois
Manufactured in Canada by Powerlite Devices, Ltd., Toronto, Ontario.

TYPE "SM"
POWER FUSES

Physicals FOR CAPACITORS!

X-Ray Photo of Solar Molded Domino Capacitor

SOLAR DOMINO TYPE MPW

There's an inside story of capacitors, too.

The genius of Roentgen is helping select the A-1's, not only in the field of medicine but also for quality inspection of molded capacitors.

Today, fluoroscopic inspection is an established procedure at Solar, assuring accurate centering of windings in Domino Molded Paper Capacitors. This "ounce of prevention" guarantees capacitors which truly reflect "Quality Above All"!

Solar Manufacturing Corporation, Bayonne, New Jersey.

Solar SOLAR CAPACITORS

ARMY E NAVY

CAPACITORS: ELECTROLYTIC • MICA • PAPER • TRIMMER • TRANSMITTING

BRUSH PROBLEMS DON'T STAY ANSWERED

. . . because Stackpole Engineering Progress makes yesterday's developments out-of-date today

CHECK THIS LIST AGAINST YOUR NEEDS

Brushes • Anodes • Bearings • Brazing Blocks • Brake Lining • Electrodes • Packing, Piston, and Seal Rings • Pipe • Rheostat Plates and Discs • Welding Rods, Electrodes and Plates, etc.

. . . also All Carbon, Graphite, Metal and Composition Contacts

STACKPOLE ELECTRONIC COMPONENTS

Fixed and Variable Resistors
Iron Cores • Switches

It pays to check brush requirements for all types of rotating electrical equipment at frequent intervals against the latest that Stackpole engineering may have to offer.

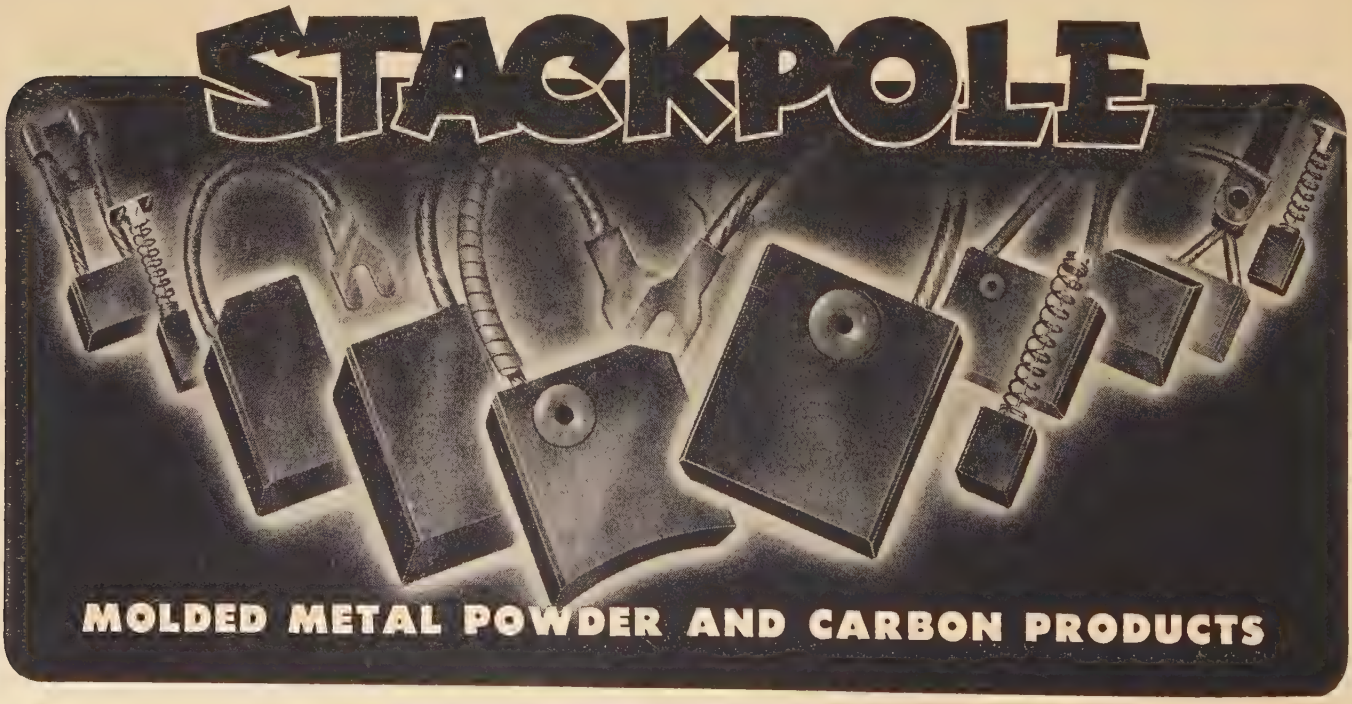
Why? Well, simply because brush engineering is a fast-moving business, and Stackpole engineering is the fastest-moving of all.

One after another, over a period of years, Stackpole brush developments have been matching the need for higher speed operation, better commutation, and higher capacity. Today, under impetus of war requirements, this work has been greatly intensified and broadened into new fields such as brushes for high-altitude work and various others.

All of which means that brush problems don't stay answered. What might have been good enough before, may no longer be good enough by comparison with recent Stackpole developments.

Stackpole brushes are sold only to manufacturers of original equipment. Inquiries will receive prompt attention.

STACKPOLE CARBON COMPANY, ST. MARYS, PENNSYLVANIA


STACKPOLE
MOLDED METAL POWDER AND CARBON PRODUCTS

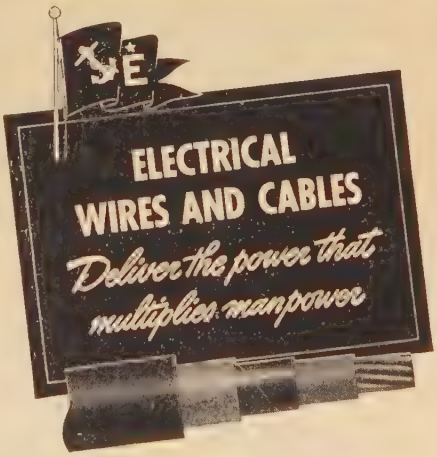
ONE of Hitler's greatest headaches is America's vast steel production—this year more by far than that of all the Axis countries combined.

The "hot seat" for Adolph is being made especially hot by America's great electric furnaces . . . for they produce the fine alloy steels that make the vital parts of our planes, guns, and ships just enough better to spell the Axis' doom.

These vitally important furnaces depend upon electric current—current which must be delivered unfailingly night and day. Only electrical wires and cables of the finest quality, of the utmost dependability, can be entrusted to this job.

We are proud that products of American Steel & Wire Company play such an important role in delivering power and light to America's vast war production machine. The quality of our electrical wires and cables is no accident. For years and years our engineering and production departments have worked for constant improvement.

And they continue to work on new ideas and developments to take care of the increased wartime demands for power and light and the ultimate conversion to peacetime requirements.


S E
ELECTRICAL WIRES AND CABLES
Deliver the power that multiplies manpower

AMERICAN STEEL & WIRE COMPANY

Cleveland, Chicago USS *and New York*

Columbia Steel Company, San Francisco, *Pacific Coast Distributors*

United States Steel Export Company, New York

UNITED STATES STEEL

Reservoir of
RESISTOR RESEARCH *for Today and Tomorrow*

Keeping in step—or a little ahead—with developments of electronic applications for Industry, Research and Communications is the day-by-day job of IRC's Engineering and Executive staffs...And while we'd like to tell you the complete and dramatic story of essential equipment in which our specially engineered components are playing vital roles today, this information must remain a military secret.

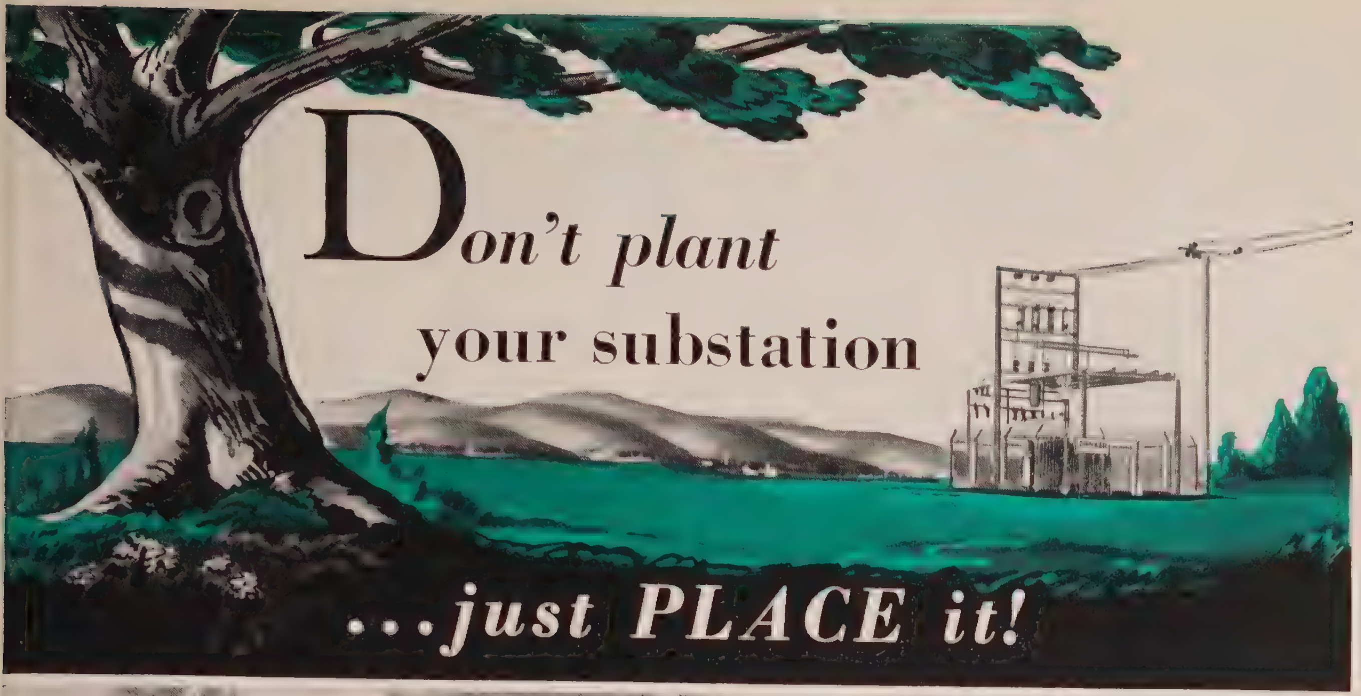
But you may be certain that as the results of IRC's wide-ranging research are brought from the files they will be found applicable and pertinent to the many challenging demands for new resistance units.

INQUIRIES INVITED

If you are up against a problem involving resistances, please feel free to call upon us for assistance. Because IRC makes more resistors of more types, in more shapes, for more applications than any other manufacturer in the world, you can depend upon receiving unbiased engineering counsel.

PREFERRED FOR PERFORMANCE
IRC
REG. U.S. PAT. OFF.
FIXED & VARIABLE RESISTORS

INTERNATIONAL RESISTANCE COMPANY
427 N. BROAD STREET · PHILADELPHIA



UNIT SUBSTATIONS

are quickly installed and easily moved

Factory-assembled sections are slid into place quickly. The substation can be moved elsewhere just as easily, should it become desirable.

WAR-PRESSED utilities and industrials are greatly accelerating the already-strong trend from *piece-meal* to *unit* substations—from *planting* a substation iece by piece to *placing* it as a unit. Unit design means quick installation, and it means you can easily nove your substation to follow load shifts.

Actually, the advantages of unit substations begin vith their selection. You can select a complete, standard ubstation as one unit, order it with a simple functional specification, and have it shipped *factory ssembled*—weeks sooner than would be possible for a iecemeal assembly.

Then, by installing these compact, safety-enclosed nits right at load centers, you can greatly shorten econdary runs, save precious copper by the ton.

Finally, you have a *co-ordinated* substation—every art designed to fit and function with every other part. Engineers see the benefits in remarkable service records. And everyone appreciates the vastly improved appearance, which gives greater flexibility of installation in urban areas.

Our attractive, new 36-page bulletin will give you useful information. Ask your G-E office for GEA-3800 or write direct to General Electric, Schenectady, N. Y.

GENERAL ELECTRIC

GAS-FILLED CABLE *take*

NOW

MEDIUM-PRESSURE "GF

HERE is an extension in the operating range of gas-filled cable that gives you all the advantages of low-pressure "GF"—the cable which has given years of satisfactory service on circuits from 10 to 40 kv. This new *medium-pressure* cable is designed for high-voltage circuits from 40 to 69 kv.

To you, the most important feature about this cable is that it is just as easy to install and maintain as is the low-pressure "GF."

How the operating range was extended

This higher operating range has been made possible by increasing the inside pressure to 45 psi, as compared with the 15 psi for the low-pressure cable. Moreover, the same high factor of safety that is built into low-pressure "GF" has been retained in this new cable.

For more detailed information on "GF" cable—

1. Write for Bulletin GEA-3652. You will want to keep this publication for your files; it contains information on what "GF" cable is, how it is used, its performance and advantages, and the accessories needed.

2. The advice and experience of G-E cable engineers are always available to you. Call on your G-E office at any time. One of our cable specialists will be glad to discuss with you this new voltage range of "GF" cable and its possibilities. *General Electric, Schenectady, N. Y.*

4 advantages of "GF" cable

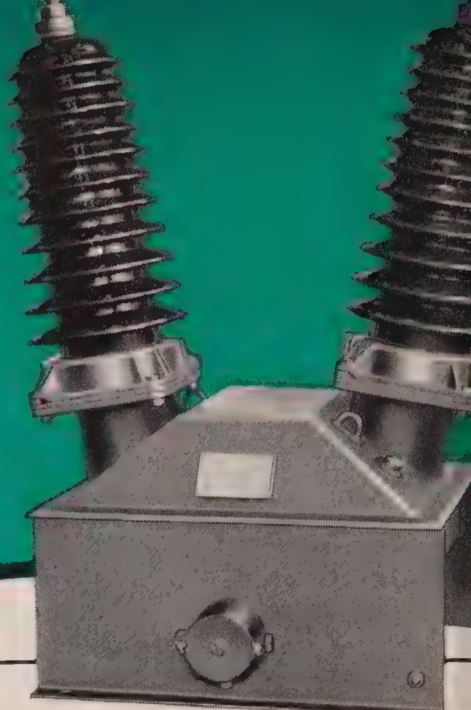
1. Easy to install—no auxiliary manhole equipment
2. Only routine inspection and maintenance—no specially trained personnel needed
3. No profile problem—no compound migration
4. Self-supervision—prompt, automatic warning if a leak occurs

GENERAL ELECTRIC

501-18-1200

You Wanted them Smaller

HOW'S THIS?



Two-bushing
Outdoor Potential Transformers
Type E-236 (Supersedes Type E-116
Pyranol or Oil-filled
24 to 69 kv

Height reduced . . . . 15 to 50 per cent
Weight reduced . . . . 10 to 60 per cent
Liquid volume reduced . . . . . 45 to 90 per cent

You Can Put Them up in the Structure...with Your Current Transforme

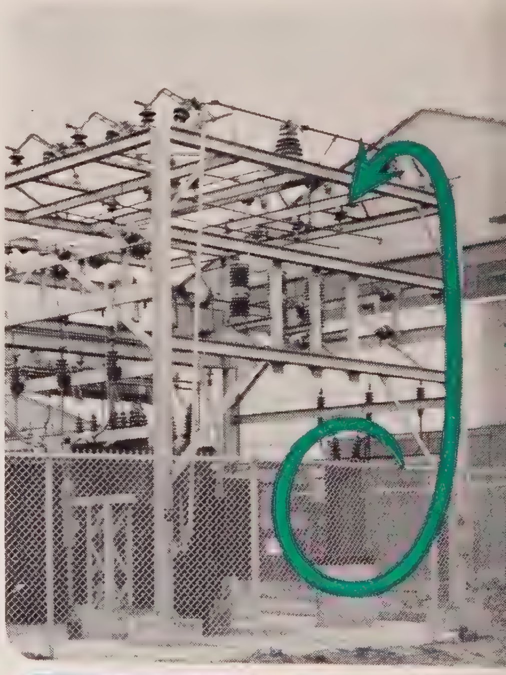
SIMPLIFY CONNECTIONS—Their light weight and small size enable you to mount them in the structure—thus simplifying primary (high-voltage) connections.

SAVE SPACE—Mounting in the structure saves valuable ground space.

SAVE FOUNDATION EXPENSE—When you put them up in the structure you don't need to construct a separate foundation.

HANDLE THEM EASIER—Their reduced size and weight make them easier to install.

REDUCE MAINTENANCE—Forget about testing or inspecting the insulating liquid; it's hermetically sealed in—there are no gaskets.



Here's How They're Built

ACCURACY—These transformers meet the highest accuracy classification—¼W, ¼X, ¼Y, ¼Z—and they're suitable for both metering and relaying.

INSULATION—Ample insulation is provided to meet the ASA standards on both impulse and low-frequency tests.

BUSHINGS—Sealed bushings eliminate gasketed joints.

TANK AND COVER—Steel plate is used, and the tank is hermetically sealed by welding the cover to it—again, no gaskets.

CORE—The core is rectangular in shape, and is similar to the *Spirakore design used in G-E distribution transformers.

***PYRANOL OR OIL-FILLED**—For the best protection to your system, you'll want to specify Pyranol—General Electric's noninflammable cooling and insulating liquid. It has high dielectric strength and is chemically stable.

**Reg. U. S. Pat. Off.*

The Army-Navy "E", for Excellence in the manufacture of war equipment, now flies over six G-E plants employing 100,000 men and women.

WANT MORE INFORMATION

If you'd like all the facts about these ne transformers, ask for Bulletin GEA-308

If you'd like to discuss the application o instrument transformers to a specific mete ing or relaying job, the meter specialist i the nearest G-E office will be glad to wor with you. Remember, there is a G-E instru ment transformer for every purpose. *Genera Electric, Schenectady, N. Y.*

GENERAL ELECTRIC

604-11-6400

QUALITY CONTROL

in the GENERAL RADIO Standardizing Laboratory

However well designed an instrument may be, accurate calibration and reliability in service determine its ultimate usefulness. Testing, therefore, has long been an important final step of our manufacturing; approximately 10% of the total man hours required to produce a General Radio instrument is spent in our standardizing laboratory. Here a carefully planned schedule of tests and measurements transforms an unadjusted, uncalibrated device into a precision instrument.

Testing specifications embody not only the rigid requirements imposed by the design objectives of the instrument, but also the field data collected in hundreds of case histories of similar instruments. Engineering test and calibration operations cover far more than meter reading and embrace a wide variety of precise electrical measurements.

To carry out these tests, capable personnel, adequate test equipment, and reliable standards are necessary. Many of the staff have engineering degrees or are graduates of engineering institutions. All are capable technicians. The laboratory equipment includes the entire line of General Radio instruments as well as those of many instrument manufacturers in other fields. As a basis for the measurements, the laboratory maintains precise, accurately-known standards of resistance, capacitance, inductance and voltage. Frequency measurements are based on the engineering department's primary standard.

Quality control in the General Radio Standardizing Laboratory is the result of years of experience in instrument manufacture; it is the customer's assurance of uniformly accurate and reliable instruments for his own testing department.

GENERAL RADIO COMPANY · Cambridge, Massachusetts
NEW YORK LOS ANGELES

OHMITE
Rheostats
For Control of Welding Apparatus
TO BATTERY
TO INDICATOR
For Remote Position Indication
For Control of Instruments and Test Apparatus
For Control of Lamp Dimming
For Control of Electronic Tubes
For Control of Current and Voltage
For Control of Motor Speed and Generator Fields

Widest Range of Sizes for Every Rheostat Need

Today's critical service requirements further emphasize the basically sound design of Ohmite Rheostats. The all-porcelain vitreous enameled construction and other time-proved Ohmite features provide *permanent* smooth, close control in countless applications . . . control of motor speed and generator fields, of electronic tubes, instruments and test equipment, of welding apparatus, lamp dimming, remote position indication, and many other devices.

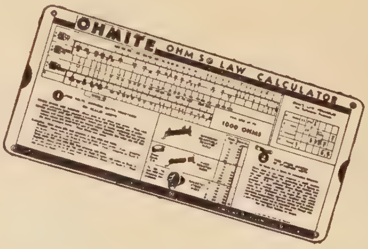
The Ohmite series of power rheostats is the most extensive today, so complete as to make it easy to select the best size for every need. There are ten sizes ranging from 25 to 1000 watts—from 1$\frac{9}{16}$″ diameter to 12″ diameter—in straight or tapered winding—in single or tandem units—in regular or special designs—to provide the exact unit for each application. Many stock types. Units produced to Government specifications.

Ohmite Engineers are glad to work with you on any problem to facilitate the design of new devices for today—and *tomorrow*. Write on company letterhead for Catalog and Engineering Manual No. 40.

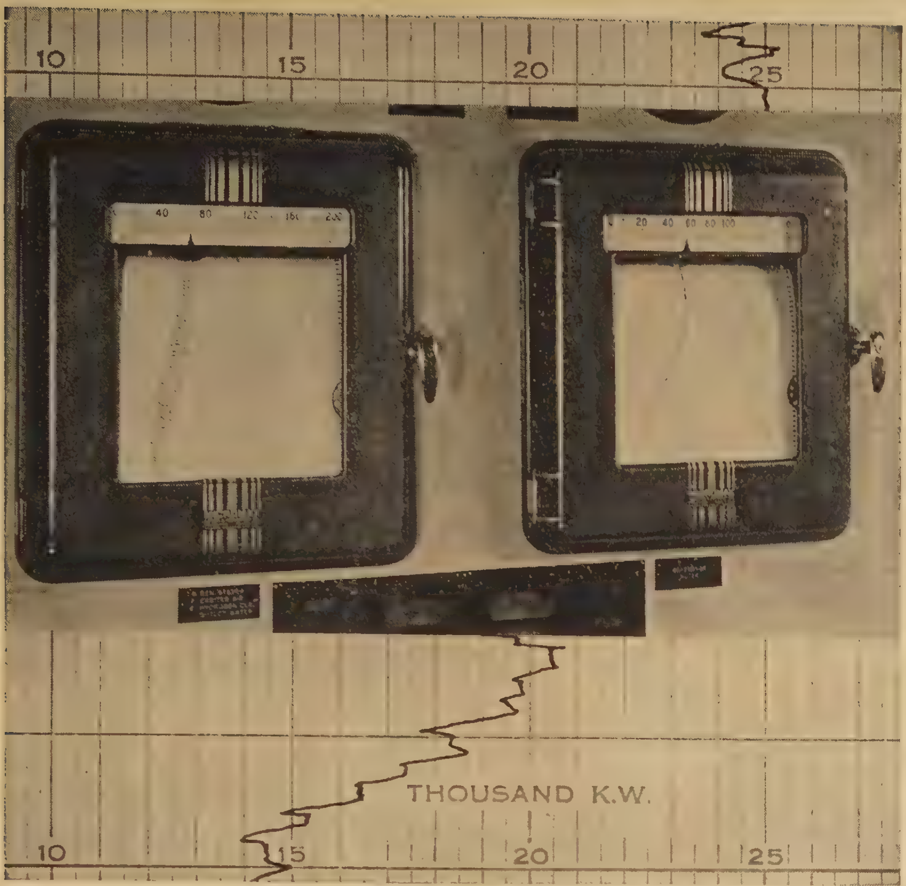
BE RIGHT WITH
OHMITE
RHEOSTATS · RESISTORS · TAP SWITCHES

Send for Handy Ohmite Ohm's Law Calculator

Thousands of these Ohmite Calculators are in practical use today. Figures ohms, watts, volts, amperes—quickly, easily. Solves any Ohm's Law problem with one setting of the slide. All values are direct reading. Send only 10c in coin to cover handling and mailing. (Also available in quantities.)



OHMITE MANUFACTURING COMPANY, 4803 **Flournoy Street, Chicago, Ill., U. S. A.**



When load begins to rise, as shown by this Micromax load record, the temperature recorders shown in the insert help watch over generator temperatures. Multiple-point Micromax at left records stator temperatures; single-point instrument at right records rotor temperature.

V depends on me!

A Slogan For All Americans

WHEN OVERLOAD PILES UP

Micromax Temperature Recorders Help Operators

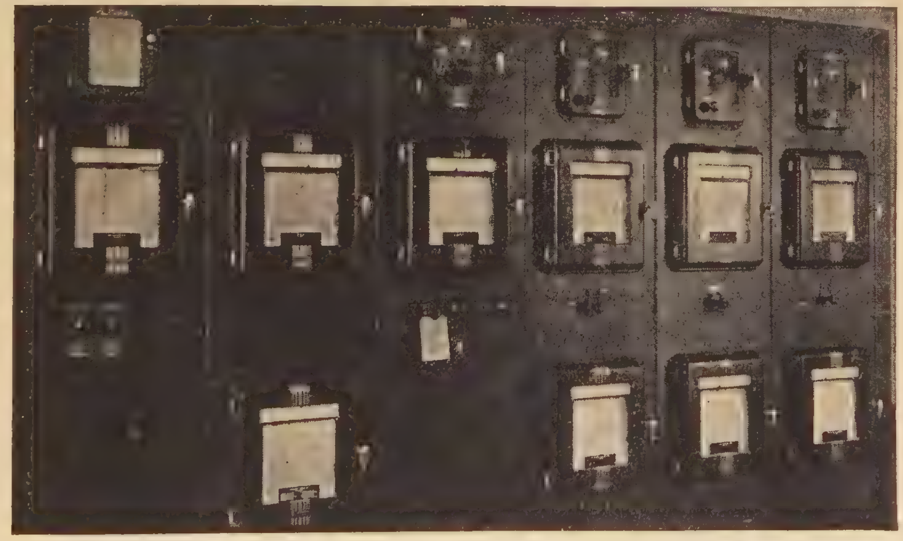
Today, when the burden of carrying maximum load requires power plants to take advantage of every factor at their disposal, Micromax Temperature Recorders are helping to solve several operating problems. Temperature—the condition which determines ultimate load—is used by the operators to show them the net effect of the load changes which are also indicated by ammeters, voltmeters and other instruments.

The operator, guided by these readings, can push overload right up to the highest safe temperature without risk to insulation. When generators are cut in, the danger of loading too rapidly is minimized; nor need the operator waste precious minutes to be safe. He sees the exact temperature increase of rotor and stator on the Micromax record as he increases load. When a generator is taken off the line and must be cooled slowly, he can watch the rate of cooling so that copper and insulation may contract without excessive strain.

Equipping the Micromax instrument to sound an alarm as soon as any temperature reaches a predetermined maximum, leaves the operator free for other duties yet insures his attention the minute it's needed.

Rotor temperature recorders are single-point instruments. For recording stator temperatures, however, 8-point Micromax instruments are used, allowing two measurements for air or hydrogen and six for stator coils.

For further details, we'll be glad to send Catalog N-33-161 *Micromax Temperature Instruments for Electric Power Equipment* on request.



On this control panel for a large hydro station are Micromax temperature recorders for fields and stators of three generators. Shown also are Micromax transformer temperature recorders, and instruments for load-frequency control.

LEEDS & NORTHRUP COMPANY, 4962 STENTON AVE., PHILA., PA.

LEEDS & NORTHRUP

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Jrl Ad N-33-161(3a)

Calibrations on meter face are easily read

MODEL 500A ELECTRONIC FREQUENCY METER

Model 500A is a direct reading Electronic Frequency Meter to read the frequencies in the range between 10cps and 50kc. At the higher frequencies you can measure the frequency difference between two radio frequency signals. In crystal grinding you can quickly measure the deviation from standard. As with all *-hp-* instruments, simplicity and speed of operation plus high accuracy are prime virtues. Excellent accuracy; plus or minus 2% of the full scale value: Good sensitivity; variation of the input voltage from 0.5 volts to 200 volts will affect the meter by not more than plus or minus 1%. Readings are independent of line voltage since a variation from 105 volts to 125 volts will affect the meter by not more than plus or minus 1%.

Get full particulars about this new *-hp-* instrument today. Data sheets will be sent you promptly and without obligation. Ask, also, for your copy of the new *-hp-* catalog which gives full information about other *-hp-* instruments and explains many standard tests and measurements.

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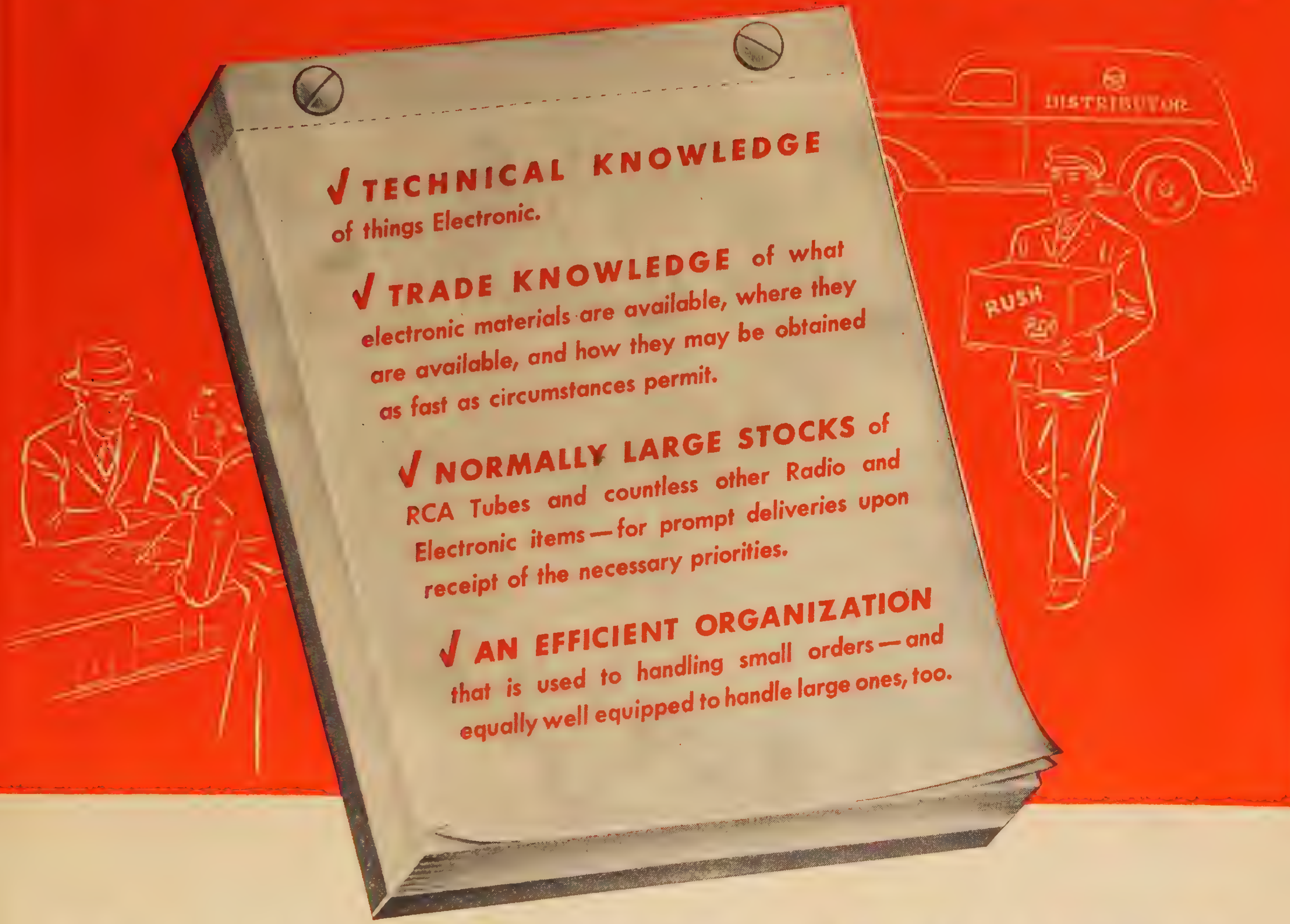
BUY U. S. WAR BONDS AND STAMPS

Your local *RCA TUBE and EQUIPMENT DISTRIBUTOR*



√ **TECHNICAL KNOWLEDGE** of things Electronic.

√ **TRADE KNOWLEDGE** of what electronic materials are available, where they are available, and how they may be obtained as fast as circumstances permit.

√ **NORMALLY LARGE STOCKS** of RCA Tubes and countless other Radio and Electronic items—for prompt deliveries upon receipt of the necessary priorities.

√ **AN EFFICIENT ORGANIZATION** that is used to handling small orders—and equally well equipped to handle large ones, too.

offers You *THESE SERVICES*

'Round the corner from your plant is a man whose chief stock in trade these days is his ability to help busy war industries get needed Radio and Electronic materials faster and use them to better advantage.

That man is your local RCA Tube and Equipment Distributor. Behind him is an organization which has been dealing with things Electronic since the "knee pants" days of Radio.

They know how to get the materials you need. They know how to use these properly once they've been obtained. You'll find their technical advice and recommendations as big a help as their personalized expediting service which assures fast, accurate deliveries.

Whether it's one RCA Tube or a hundred—they're ready and geared to help you. They'll help you, too, in obtaining condensers, resistors, rheostats, controls, potentiometers, coils, transformers, wire, and numerous other Radio and Electronic components produced by many manufacturers.

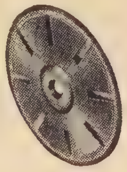
RCA RADIO-ELECTRONIC TUBES

RCA Victor Division, **RADIO CORPORATION OF AMERICA**, Camden, N. J.

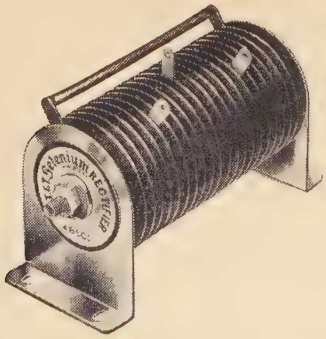
Complete Equipment
Design and Manufacturing Service

producing Battery Chargers, Power Supply Units and other Special Equipment powered by I.T.&T. Selenium Rectifiers

Special Assembly Method — *showing single metal washer which facilitates protective coating against corrosion*



Standard Assembly Method—*showing conventional petal-shaped brass contact washer*



Now—if you are a manufacturer of electrical equipment for military use—we offer you a complete design and manufacturing service, producing Selenium Rectifier power supply units for use with your equipment.

And, if your production lines require D.C. power, we can design Selenium Rectifiers for any power range. Units available with either the widely used standard assembly or the new special assembly, coated for protection against marine and other high humidity services.

All equipment powered by long-life, trouble-free I. T. & T. Selenium Rectifiers—accepted as standard by the electrical industry.

Consulting Engineering Service available. For descriptive bulletins address Technical Department.

SELENIUM RECTIFIER DIVISION

Federal Telephone and Radio Corporation

AN IT&T ASSOCIATE

1000 Passaic Ave.
East Newark, New Jersey

BOEING STRATOLINER

Constant voltage protection all the way

Ask the men who produce planes and the men who pilot them. They'll tell you what vital part *constant voltage* plays in modern aviation. In the sky, it's *constant voltage* on the directional beam which guides the ships through night and storm. In the shop, it's *constant voltage* on the production line which maintains the split-hair accuracy of precision airplane parts.

For the aircraft industry—*and for your own*—SOLA CONSTANT VOLTAGE TRANSFORMERS provide this all-important stabilized power. They stand between costly equipment and destructive voltage fluctuations now common on overloaded power lines. Without supervision they instantly absorb power sags and surges as great as 30%.

For unerring operation of precision tools, and protection of almost irreplaceable instruments and electronic tubes, put SOLA CONSTANT VOLTAGE TRANSFORMERS on duty in your plant. They're built in standard units from 10 VA to 15 KVA capacity—self-protecting against short circuit and without moving parts. Special units can be built to specification.

Note to Industrial Executives: *Find out how Sola "CV" transformers can solve voltage control problems in your operations. Send for bulletin ACV-74.*

SOLA

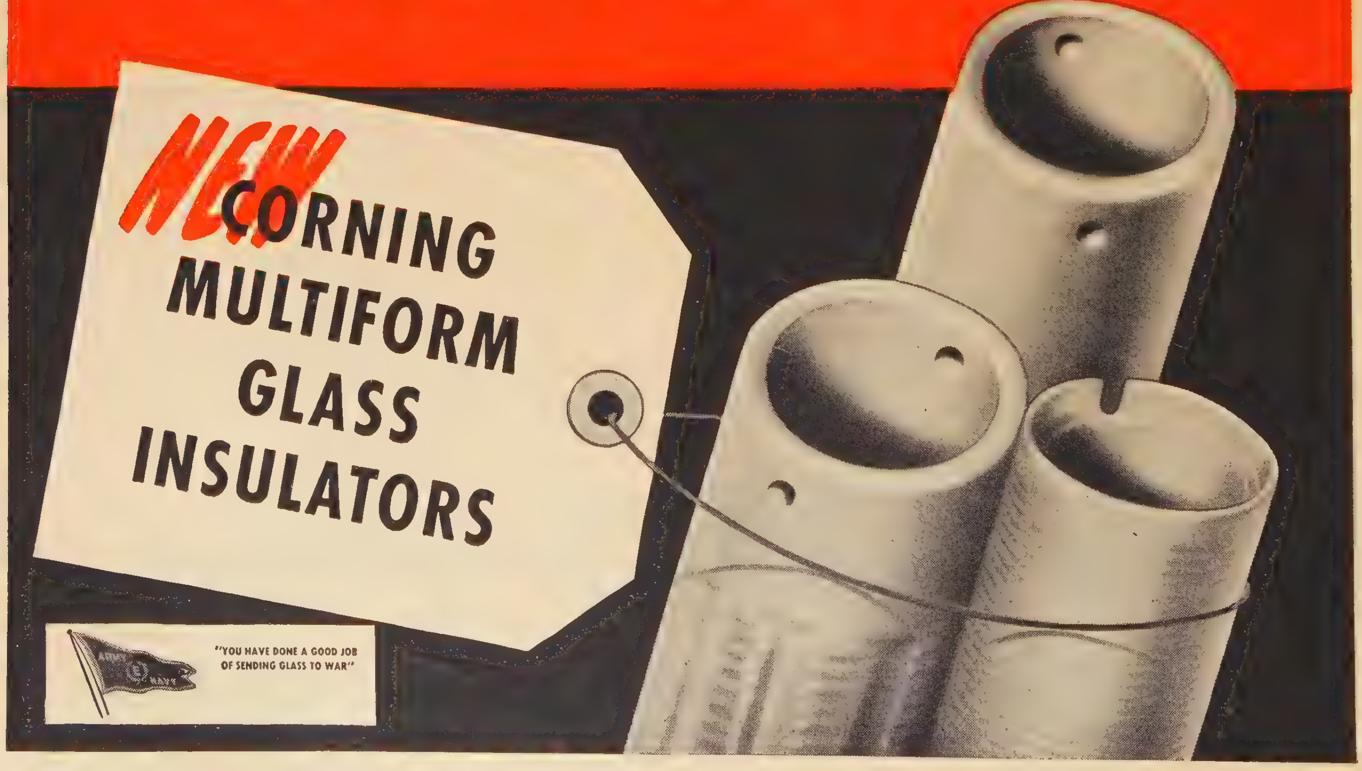
Constant Voltage Transformers

Transformers for: Constant Voltage • Cold Cathode Lighting • Mercury Lamps • Series Lighting • Fluorescent Lighting • X-ray Equipment • Luminous Tube Signs Oil Burner Ignition • Radio • Power • Controls • Signal Systems • Door Bells and Chimes • etc. **SOLA ELECTRIC CO., 2525 Clybourn Ave., Chicago, Ill.**

HOW TO SAVE

UP TO 47%

ON ELECTRICAL COIL FORMS!



HERE'S ALL YOU DO TO MAKE ROCK BOTTOM SAVINGS

READ THIS QUICK CHECK LIST THEN MAIL COUPON FOR COMPLETE STORY!

YOU ENJOY LOWEST PRICES WHEN YOUR COIL FORMS COME WITHIN THESE STANDARDS

*1. **OUTSIDE DIAMETERS** between 1 inch and 3 inches.

*2. **WALL THICKNESSES** between 5/32 inch and 9/32 inch.

*3. **LENGTHS** up to 9 inches (with better prices for shorter lengths).

4. **MAXIMUM** of 20 holes for coil forms ¼ inch and 9/32 inch thick with maximum of 4 holes tapped.

5. **MAXIMUM** of 10 holes for coil forms 5/32 inch to 7/32 inch thick with maximum of 2 holes tapped.

6. **MAXIMUM** of 14 grooves to the inch.

*7. **TOLERANCES** on general dimensions ± 2.0%, but not less than ± 0.010 inches.

**Proposed A.S.A. Standards.*

In addition to low-cost standard types, Corning Multiform Insulators may be had in almost any other size or shape.

CLIP THIS COUPON TO YOUR BLUE PRINT - MAIL TODAY FOR ACCURATE QUOTATION!

Corning Glass Works
Insulation Division, Dept. N-67, Corning, N. Y.

Please send us estimate on coil forms as per attached blueprint and data below:

Quantity..
When Needed (date)..............................
Electrical Characteristics......................
....
Acceptable Revisions............................
....
Name..
Company...
Street..
City......................State...................

Pyrex Insulators
BRAND

"PYREX" *is a registered trade-mark and indicates manufacture by Corning Glass Works.*

CONTROL CENTERS

Fabricated by KIRK & BLUM

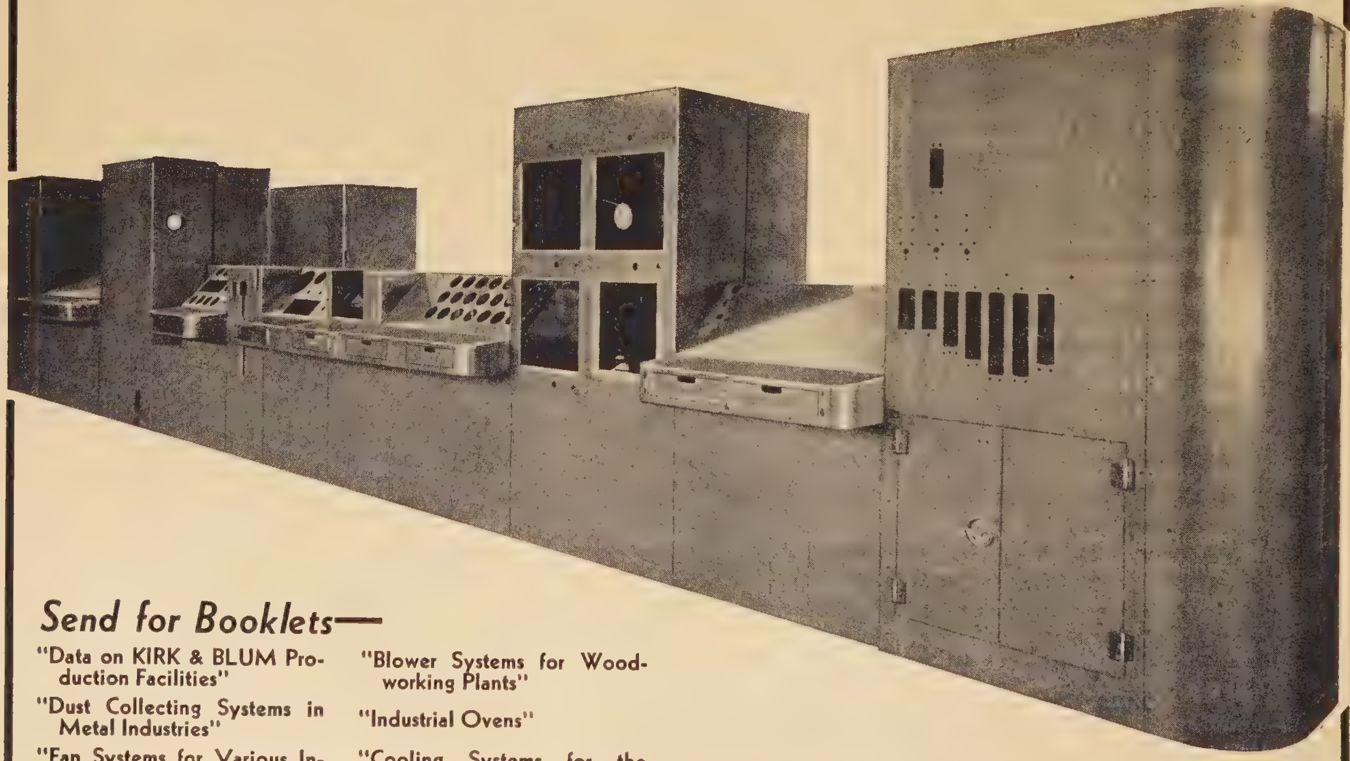
The illustration below is a series of control panels and desks for an airplane motor test room. This entire assembly is an excellent example of one of the various sheet metal products fabricated by Kirk & Blum in strict adherence to customer's design and specifications.

With a background of specialized experience of more than thirty-six years, fabricating sheet metal products for various industries, we can, with equal skill and dexterity, fabricate your special products. Our extensive modern plant facilities and a corps of skilled workmen are further assurance that your work will be completed with speed and economy.

Consult Kirk & Blum Engineers. They will analyze your problems and make recommendations to solve them to your decided advantage.

ACCURATE SHEET STEEL FABRICATION—CAPACITY $^3/_8$ INCH and LIGHTER

SEND US YOUR BLUE PRINTS for PROMPT QUOTATIONS



Send for Booklets—

- "Data on KIRK & BLUM Production Facilities"
- "Dust Collecting Systems in Metal Industries"
- "Fan Systems for Various Industries"
- "Blower Systems for Woodworking Plants"
- "Industrial Ovens"
- "Cooling Systems for the Glass Industry"

THE KIRK & BLUM MANUFACTURING CO.
AN ORGANIZATION OF ENGINEERS AND MECHANICS
2855 SPRING GROVE AVE. CINCINNATI, OHIO

A DROP OF PROTECTION

This little drop with billions of others, taken collectively, forms the impervious film which protects the giant transformers, generators and motors of American industry as well as the special electrical units required by our Armed Forces. Such is insulating varnish—although only a few mils thick, it provides the necessary protection to electrical units.

Perhaps you do not know just what type of varnish to specify for treatment of your newly designed units. It is a very complex problem as varnish insulation and treatment which may prove very successful for one type of an electrical unit may be entirely unsatisfactory for another.

We maintain a fully equipped laboratory for those who are confronted with an insulating varnish problem. We are confident, no matter what your problem may be, that with our years of research and specialization in the field of insulating varnishes, we can help you. There are no obligations, so why not let us assist you?

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CHINALAK Insulating Varnishes
SYNTHITE Insulating Varnishes
Stator Compounds
Filling Compounds
Cable Enamels
Radio Waxes
Melting Compounds
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Dolph's

JOHN C. DOLPH COMPANY

Insulating Varnish Specialists

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VARNISHED CAMBRIC • RUBBER POWER CABLES • BUILDING WIRE • RADIO WIRES • SIGNAL CABLE • FLEXIBLE CORDS • LEAD-ENCASED AND PARKWAY CABLES • ARMORED CABLE

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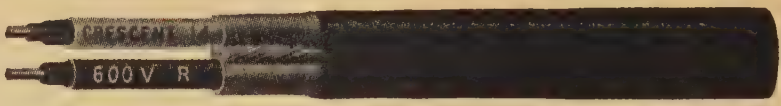
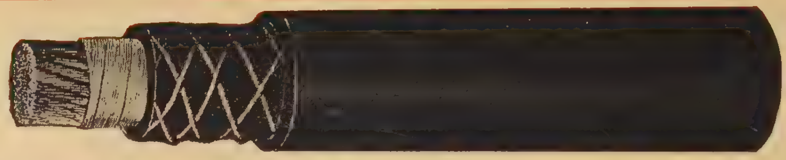
CRESCENT
ELECTRICAL WIRES AND CABLES
That Are Meeting Today's Special Needs

For war housing, industrial wiring and interior wiring of all kinds.



"CRESFLEX" Non-Metallic Sheathed Cable

Extensively used in shipyards, and for the manufacture of tanks, guns and planes.



"L" Welding Cable

For portable power leads to drills, tools, welding and construction machinery.

"PERMACORD" Heavy Duty Portable Cable

For electric power and control circuits aboard ship.

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For general industrial power wiring. Widely used because no rubber required in construction, and can carry larger currents for same size copper conductors.

Varnished Cambric Power Cable

CRESCENT INSULATED WIRE & CABLE CO.

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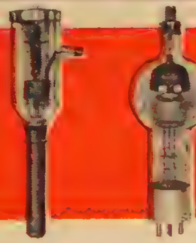
WIRE and CABLE
Factory: TRENTON, N. J.—*Stocks in Principal Cities*

CRESCENT ENDURITE SUPER-AGING INSULATION • WEATHER-PROOF WIRE

CRESFLEX NON-METALLIC SHEATHED CABLE • SERVICE ENTRANCE CABLE • MAGNET WIRE • BARE WIRE

No. 3 OF A SERIES EXPLAINING THE USES OF ELECTRONIC TUBES IN INDUSTRY

Electronic heating will do it in seconds instead of minutes



Two electronic tubes, the G-E phanotron and the G-E pliotron, supply the high-frequency waves used in electronic heating.

BY ELECTRONIC heating, small gears can now be case-hardened to a predetermined depth in a few seconds. The hardness pattern may be rigidly controlled as to shape and size. The whole rim of teeth, the tooth tips or the tips and sides of the teeth may be hardened as desired.

In other electronic heating applications, a metal rod is brazed to its metal bushings and terminals in 11 seconds . . . in another, a metal shell is soldered to its base in 3 seconds . . . and in a third case, a glass tube is fused to a metal base in air-tight, permanent bond in 20 seconds. Scores of new wartime uses are showing the advantages of uninterrupted operation, high speed, and quality production.

G-E electronic tubes have two functions in electronic heating. The G-E phanotron supplies the direct current. The G-E pliotron converts this current to high-frequency waves and creates the heating field.

This field may be localized . . . precisely controlled as to amount, direction, or area limit. Few heating methods offer industry such flexibility, accuracy and uniform results.

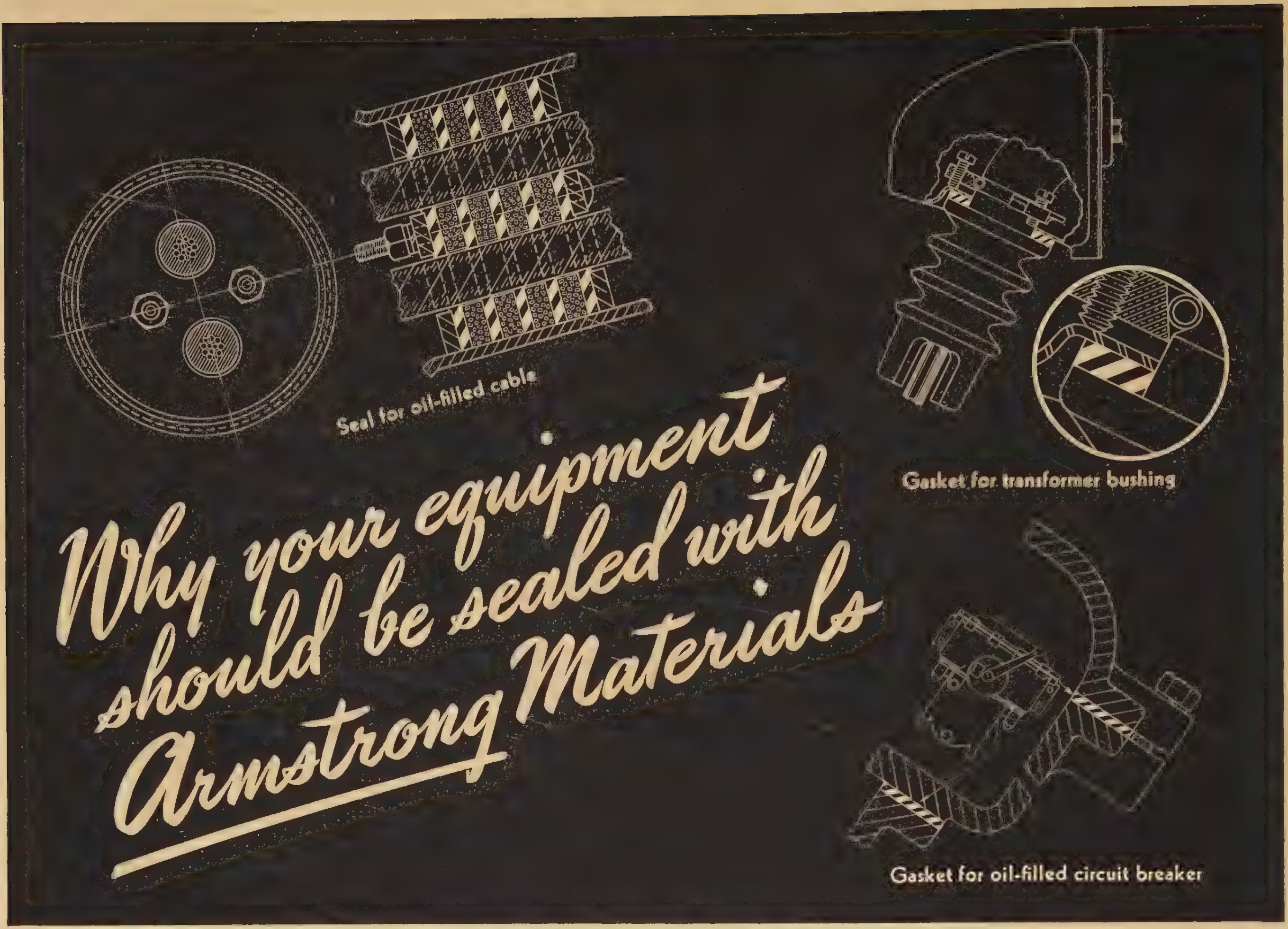
It is the purpose of the G-E electronic tube engineers to aid any manufacturer of electronic devices in the application of tubes. General Electric, through its nation-wide distribution system, is also prepared to supply users of electronic devices with replacement tubes.

Also, we will be glad to place interested men in your plant on our mailing list. For example, we will send a full-color spectrum chart showing electronic tubes and applications. Write on company letterhead. *Electronics Dept., General Electric, Schenectady, New York.*

On Sunday evening listen to the General Electric Mazda Lamp program over the N. B. C. network. See newspapers for time and station.

GENERAL ELECTRIC

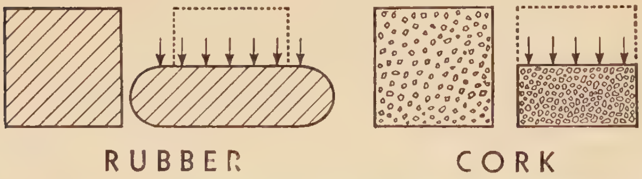
162-86-8850


Why your equipment should be sealed with Armstrong Materials
Seal for oil-filled cable
Gasket for transformer bushing
Gasket for oil-filled circuit breaker

FOR years, electrical equipment manufacturers have relied upon the Armstrong Cork Company for solutions to their sealing problems.

Armstrong makes more than 50 sealing materials, including synthetic rubbers, cork-and-synthetic-rubber compositions, and cork-and-rubber compositions. Armstrong's sealing specialists know how to determine the *right* material for any sealing job.

For example, you may need a tough, resilient, oil-resistant, weatherproof gasket that will compress without side flow. Synthetic rubber meets all these requirements except one: it flows under pressure. Cork, on the other hand, is truly compressible. It does not flow laterally, but changes its shape only in the direction of the applied force. Armstrong combines cork and synthetic rubber to produce compositions which provide cork's compressibility plus synthetic rubber's mechanical strength; resistance to oils, solvents, sunlight, corona; and imperviousness to liquids and gases.



For services where there is no need for true compressibility, or where lateral flow is desired, Armstrong makes materials that contain no cork.

Rolls, Sheets, Special Shapes

Armstrong Compositions are available in rolls, sheets, gaskets, and molded or extruded shapes—for sealing transformers, switches, potheads, oil-filled cables, circuit breakers, condensers, and other electrical equipment. For practical, unbiased recommendations, send your sealing problems to Armstrong Cork Company, Industrial Division, 4006 Arch Street, Lancaster, Pa.

ARMSTRONG'S GASKETS · SEALS · PACKINGS

Synthetic Rubbers • Cork-and-Synthetic-Rubber Compositions* • Cork-and-Rubber Compositions
Cork Compositions • Fiber Sheet Packings • Rag Felt Papers • Natural Cork
*FORMERLY "CORPRENE"

"Just getting the wire laid was a tough problem. Keeping it intact in bombings, shellings and adverse weather is a twenty-four-hour proposition. . . . Wire repair crews are made up of four men. Three stand guard while the other works."
(From story by Sgt. James W. Hurlbut, Marine Corps Combat Correspondent)

Telephone Exchange on Guadalcanal

Marine communications men built it under fire. And it has been kept built. The "Guadalcanal Tel & Tel" covers well over a thousand miles of wire.

That is where some of your telephone material went. It's fighting on other fronts, too. We're getting along with less here so they can have more over there.

Telephone lines are life-lines and production lines in a war. Thanks for helping to keep the Long Distance wires open for vital calls to war-busy centers.

WAR CALLS COME FIRST

BELL TELEPHONE SYSTEM

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UNITED STATES

ORDINARY WIRE

1 TRUCK FOR U.S. Laytex ASSAULT WIRE

REG. U. S. PAT. OFF.

4 TRUCKS RELEASED FOR OTHER MATÉRIEL TRANSPORT

Every day our Armed Forces are in the field, distances from base to front lengthen... problems of transportation increase...every cubic foot of space that can be saved for the S.O.S. is vital.

U. S. Laytex Assault Wire requires only 1/5 the space in transport of ordinary communications wire.

In the unique Laytex Process, the compounded, purified latex is applied to the electrical conductor by dipping. Conductors are perfectly centered and the weight of the insulation and the diameter of the insulated conductor are kept at the minimum.

Lightweight and small bulk are only two advantages of U. S. Laytex Assault Wire. It resists concussion and a wide range of temperature changes. Laytex is waterproof and extremely flexible.

Laytex Assault Wire weighs less than 30 lbs. per mile...a mighty important feature when men are marching with full equipment. It has a talking distance of five miles and a breaking strength of 50 lbs. per conductor.

RUBBER COMPANY

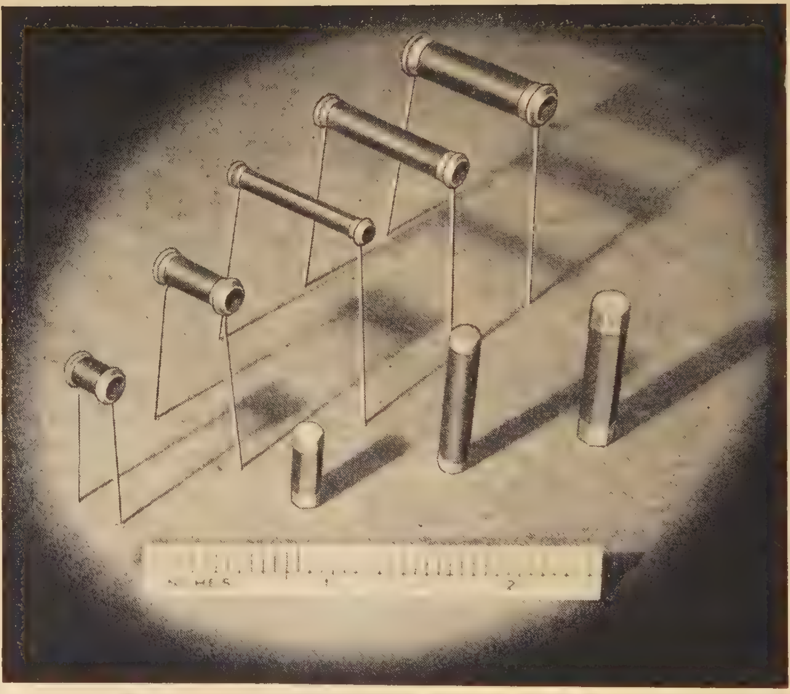
KEYSTONE'S

New Development

NEGATIVE TEMPERATURE COEFFICIENT RESISTANCE MATERIAL . . .

DEVELOPED to compensate for electrical resistance changes due to temperature variations—to reduce or eliminate initial current surge in electrical equipment—to provide various degrees of time delay in electrical units—used as a remote unit for temperature measurement, etc. . . .

Write for information.



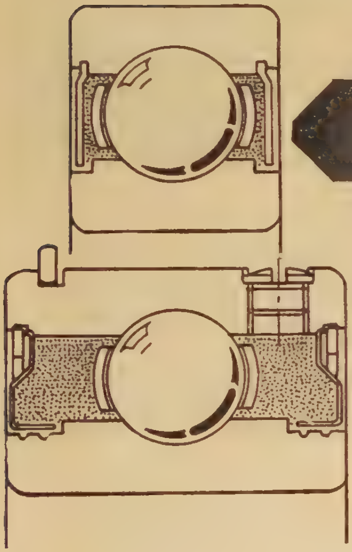
Representative units

Manufactured by

KEYSTONE CARBON COMPANY, INC.

SAINT MARYS . . . PENNA.

YOU CAN'T GET A QUART INTO A PINT MEASURE



And, "by the same token," you can't get as much grease into a single-row-width "sealed" ball bearing, as goes into the double-row-width "CARTRIDGE" BALL BEARING of equal bore and outside diameter. Look at the section drawings herewith, and you'll see why.

In the single-row-width shielded or "sealed" bearing, see that very small grease space inside the sealing plates. Note further that there is no space available for a highly-efficient, truly grease-tight seal.

Now, in contrast, see that big grease reservoir between the seals, in the "CARTRIDGE" BEARING section—providing at least DOUBLE THE GREASE CAPACITY of the shielded or "sealed" bearing, and greatly extending the greasing intervals. Note, too, how the wide, inwardly-extending seal flanges, and the oil grooves on the inner ring, provide AN IDEAL SEAL THAT PREVENTS THE ESCAPE OF GREASE AND EXCLUDES DIRT, whatever the shaft angle.

Furthermore, the "CARTRIDGE" SEALS ARE REMOVABLE AND REPLACEABLE, should you wish to inspect the bearing or change the grease. And the refilling plug makes it easy to put in a new charge of grease. You can't do this, with the ordinary shielded or "sealed" bearing, which is permanently sealed.

QUALITY, too, is just as vital as QUANTITY, in the grease in your bearings. So, every "CARTRIDGE" BEARING leaves our factory, packed with NORMA-HOFFMANN "STABILITY-TESTED" GREASE, which has great resistance to oxidation as well as excellent lubricating properties.

Summing up . . . all these distinctive features of the NORMA-HOFFMANN "CARTRIDGE" BALL BEARING mean prolonged bearing life, less lubrication attention and lower maintenance cost. Write for the Catalog, enumerating many additional advantages. Let our engineers work with you.

NORMA-HOFFMANN

"CARTRIDGE"

BALL BEARING

NORMA-HOFFMANN BEARINGS CORP'N, STAMFORD, CONN. • FOUNDED 1911

TO WIN THE WAR: *Work—Fight—Buy War Savings Bonds!*

MORGANITE BRUSHES

. . . meeting industry's vast production needs

In every type of motor, generator and dynamotor—in both main and auxiliary drives—MORGANITE brushes are vital to essential-industrial production. They have the 'guts' to consistently deliver; engineers and production men know these brushes by past performances.

MORGANITE brushes eliminate service interruptions, tie-ups and work stoppages. The remarkable results achieved for industry over the years, are now being duplicated and even surpassed for the war-effort.

MORGANITE engineers will gladly collaborate in the solution of your brush problems; your inquiries are invited.

WRITE FOR DATA BOOKLET.

The MORGANITE 30-page informative booklet is available on request without obligation. The correct type, material and application are described in detail.

MORGANITE BRUSH CO.
INCORPORATED
LONG ISLAND CITY, NEW YORK

The product of the *Pioneer*

- Aerovox introduced the high-capacity AC electrolytic capacitor for motor-starting. This development revolutionized the fractional horsepower motor art and made the moderate-priced electric refrigerator feasible.

Aerovox has pioneered in other capacitor types and applications as well, and today is still pioneering in the industrial, electronic and radio fields.

Such pioneering courage, together with exceptional engineering experience backed by unexcelled production facilities, may be yours by simply specifying "AEROVOX" for capacitors.

Send us your problem if it deals with capacitors, for engineering collaboration, specifications, quotations. Literature on request.

AEROVOX *Capacitors*

INDIVIDUALLY TESTED

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1560 EAST FIRST STREET • • • SANDUSKY, OHIO

The spirit of Lafayette is a fighting spirit. Lafayette Radio is bending every effort to speed America's war production by supplying emergency requirements of radio, sound and electronic parts to all branches of the armed forces as well as to manufacturers and subcontractors.

Make Lafayette *your* headquarters for parts. A single order to Lafayette, no matter how large or how small, will bring quick delivery on all of your parts requirements.

Free 130-Page Catalog. Radio, Sound and Electronic Parts. Write 901 W. Jackson Blvd., Chicago, Ill., Dept. 6C3.

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RADIO CORP.
901 W. JACKSON BLVD., CHICAGO, ILL.
265 PEACHTREE ST., ATLANTA, GEORGIA

"Quick Delivery on Radio, Sound and Electronic Parts"

Typical Littelfuse Universal Fuse Panel made up from B/P.

Your panels come precisely to your specifications.

Send NOW for New Blueprint of

LITTELFUSE

Universal **FUSE PANEL No. 1505**

Blueprint comes to you without charge. With it you can quickly designate the Fuse Panel for your requirements—*exactly to your specifications.* One number covers your mounting. No. 1505 specifies Standard Panel Mounting; first dash number, size of fuse required; second dash number, the number of poles required. If bus bars are required, specify separately, and specify poles to be bussed.

READY TO MOUNT

These strong light panels are equipped with terminals and Beryllium Copper Fuse Clips, or terminal studs. They meet all Air Corps requirements. They insure the utmost in durability.

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Here is important aid in saving hours of time and labor, speeding production. Send for as many copies as you need of new B/P for Littelfuse Universal Fuse Panel No. 1505. *Special panels to your specifications made promptly.*

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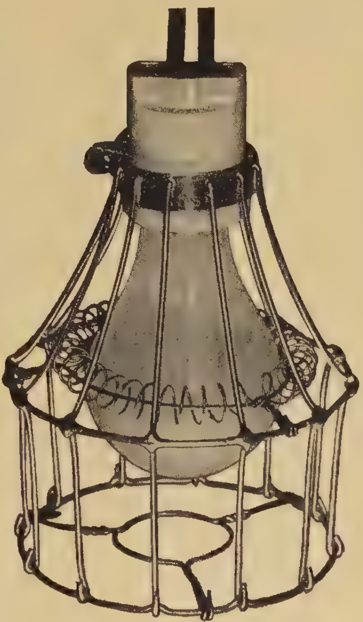


MATTHEWS HOLDFAST LAMP GUARDS

Better because they remove lamp fire hazards; they stop lamp breakage; they stop unauthorized lamp removal; they are stronger and will last longer.

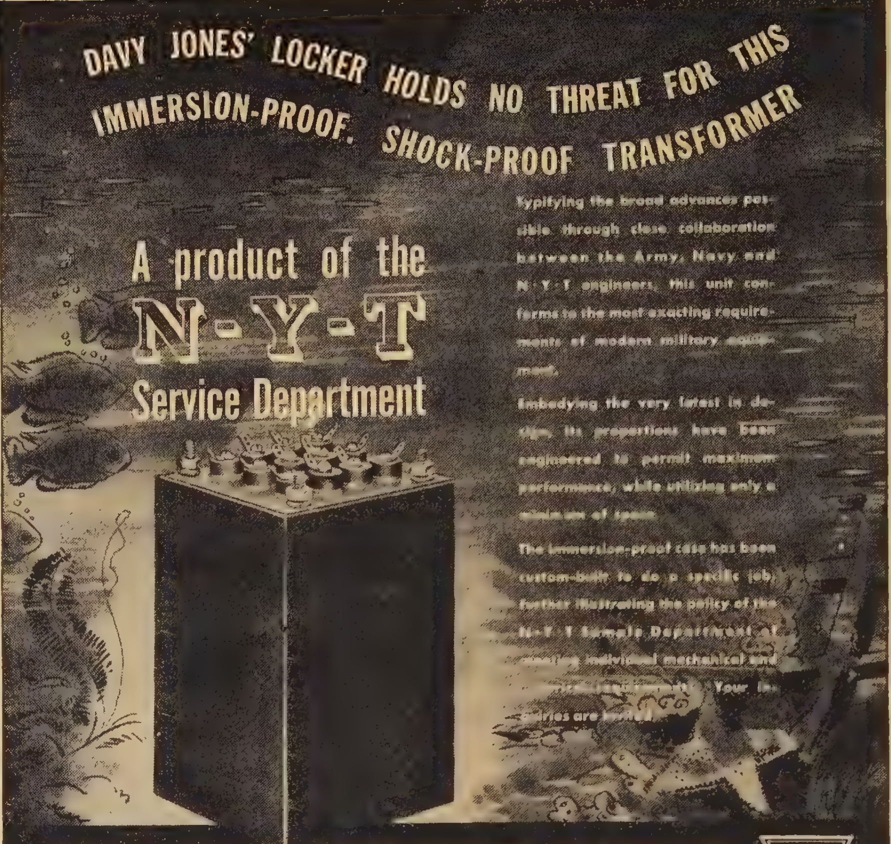
They are the only lamp guards which are guaranteed for 10 years.

They are approved by Fire Underwriters.

They are made in four sizes, of 14 bwg wire and for brass or weatherproof sockets.

Write for Bulletin A-B which will give you full details including net prices.

W. N. MATTHEWS CORPORATION
ST. LOUIS, U. S. A.


DAVY JONES' LOCKER HOLDS NO THREAT FOR THIS IMMERSION-PROOF, SHOCK-PROOF TRANSFORMER
A product of the N-Y-T Service Department

Typifying the broad advances possible through close collaboration between the Army, Navy and N-Y-T engineers, this unit conforms to the most exacting requirements of modern military equipment.

Embodying the very latest in design, its proportions have been engineered to permit maximum performance, while utilizing only a minimum of space.

The immersion-proof case has been custom-built to do a specific job, further illustrating the policy of the N-Y-T Sample Department of meeting individual mechanical and electrical requirements. Your inquiries are invited.

NEW YORK TRANSFORMER COMPANY
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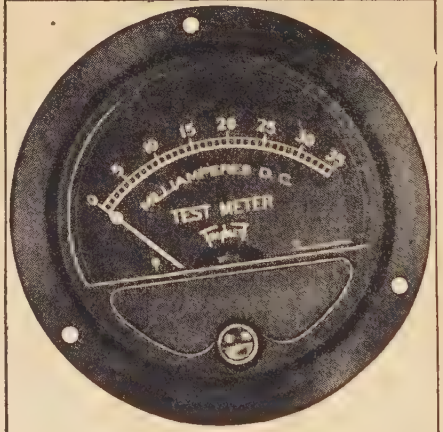
TRIPLETT
Combat Line
INSTRUMENTS



Thin Line MOD

TRIPLETT

Deliveries under government requirements are facilitated by two large sub-contracting organizations combining to increase Triplett output.
TRIPLETT ELECTRICAL INSTRUMENT CO., BLUFFTON, OHIO

Protection and Safety with
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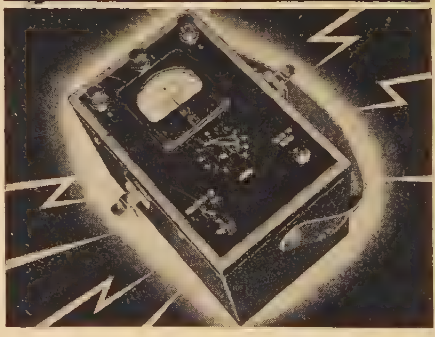

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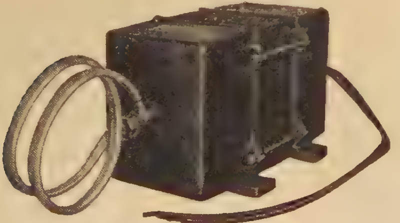

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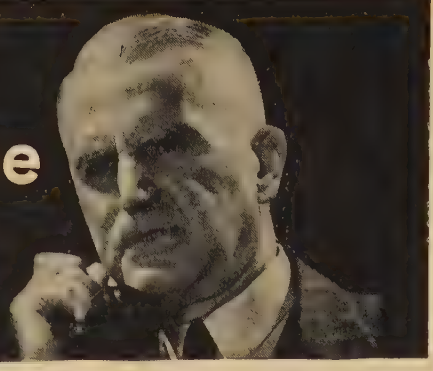

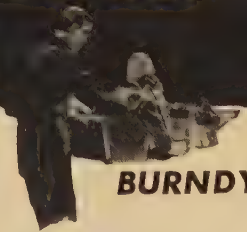

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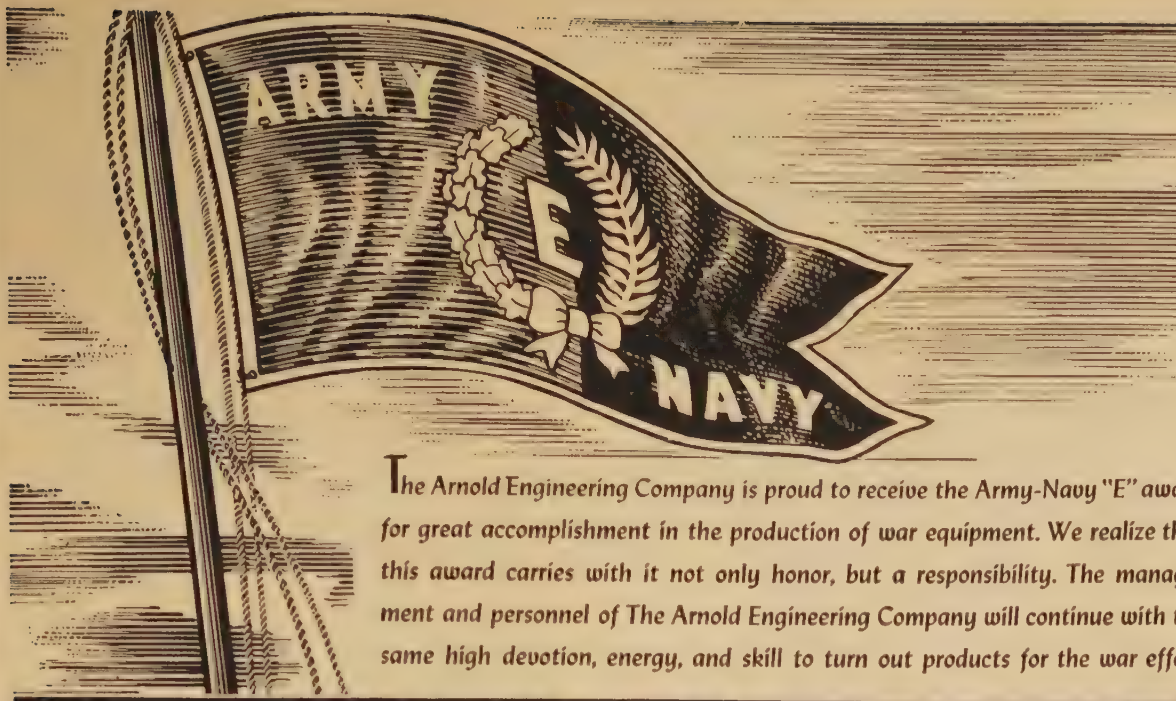

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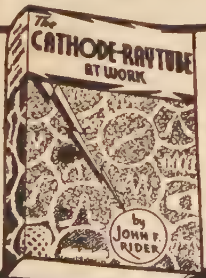

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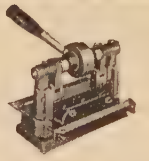

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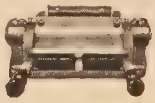

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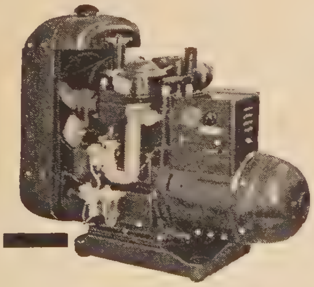

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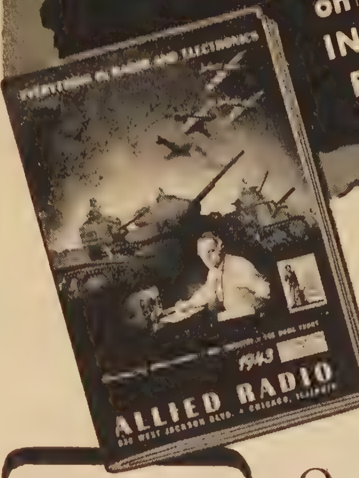
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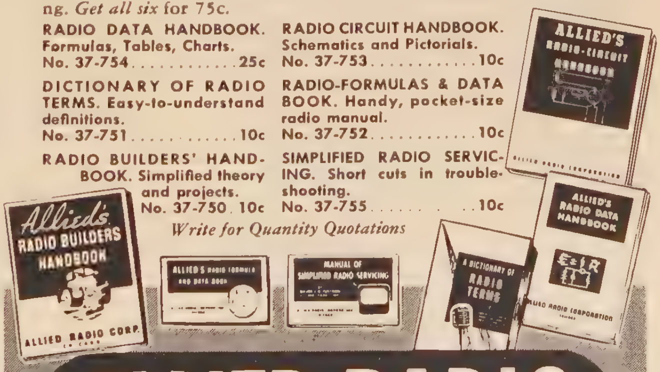
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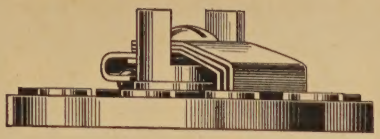

This is the shorting type. As the arm is rotated from one position to another the adjacent contact points are "shorted" (bridged).

or

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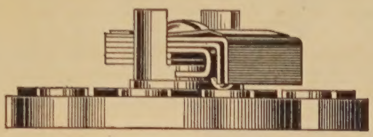
This is the non-shorting type. As the arm is rotated from one position to another, the arm lifts up, and only one contact is touched at a time.



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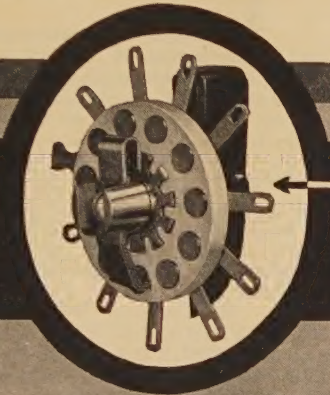

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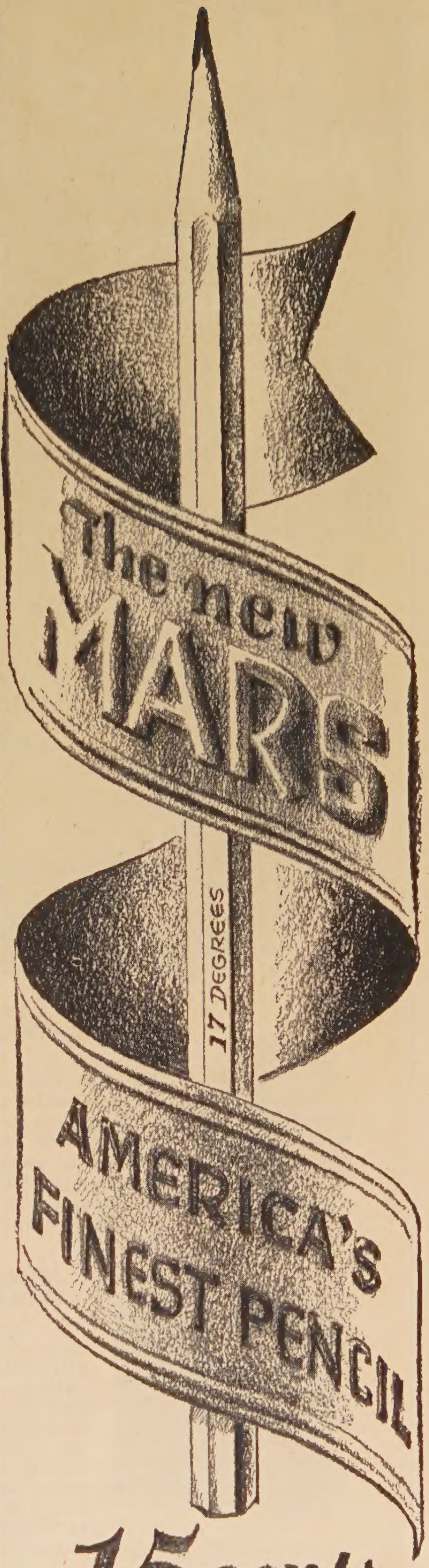

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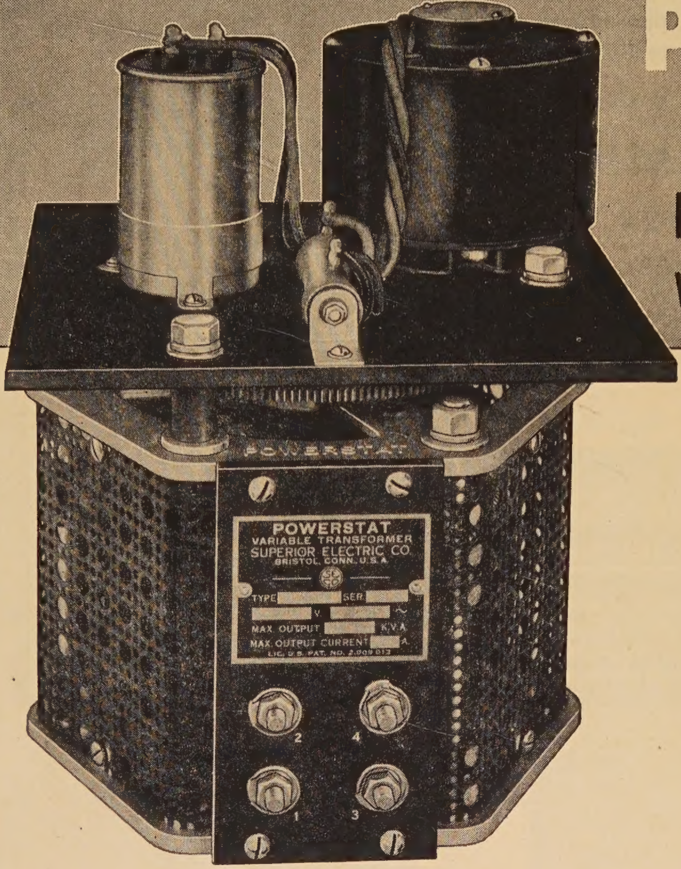

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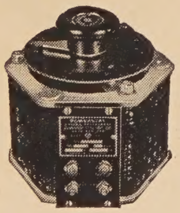

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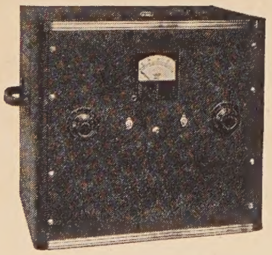

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